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Linear Motion Alternators (LMAs)

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12.1 Introduction

This chapter deals mainly with linear oscillatory motion electric generators. The linear excursion is within a few centimeters. As prime movers, free piston Stirling engines (SEs), linear internal combustion single-piston engines, and direct wave energy engines (with meter range excursions) are proposed.

So far, the Stirling engine [1] has been used for spacecraft and for residential electric energy generation (Figure 12.1) [1]. The linear internal combustion engine (ICE) was recently proposed for series hybrid electric vehicles.

While rotary motion electric generators are multiphase machines, in general, the linear motion alternators (LMAs) tend to be single-phase machines, because the linear oscillatory motion imposes a change of phase sequence for change in the direction of motion. A three-phase LMA may be built of three single-phase LMAs.

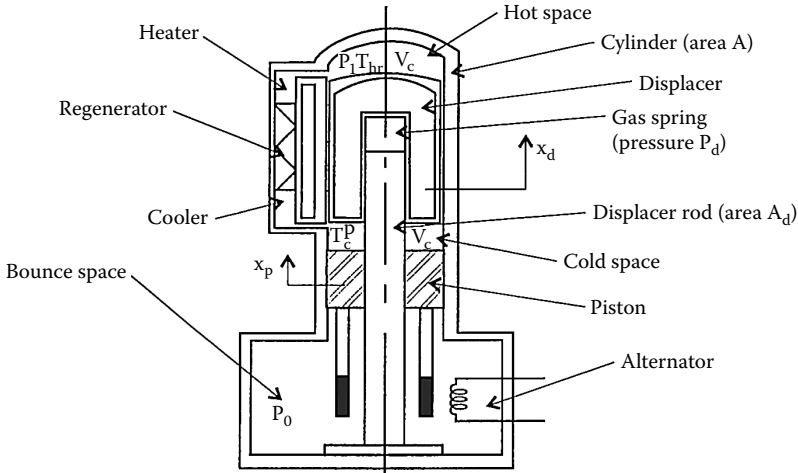


FIGURE 12.1 Stirling engine — linear alternator.

Though at first LMAs with electromagnetic excitation were proposed as single-phase synchronous generators (SGs), more recently, permanent magnet (PM) excitation took over, and most competitive LMAs now rely on PMs.

A brief classification into three categories may be useful:

- With coil mover (and stator PMs)
- With PM mover
- With iron mover (and stator PMs)

One configuration in each category is treated separately in terms of principle and performance equations. The dynamics and control will be treated once for all configurations. A special kind of linear motion generator with progressive motion to provide on-board energy on maglev (magnetically levitated) vehicles with active guideway is also briefly discussed.

12.2 LMA Principle of Operation

PM-LMAs with oscillatory motion are, generally, single-phase machines with harmonic motion:

$$x = x_m \sin \omega_r t \quad (12.1)$$

The electromagnetic force (emf) is, in general,

$$e(t) = - \frac{\partial \Psi_{PM}}{\partial x} \frac{dx}{dt} \quad (12.2)$$

where Ψ_{PM} is the PM flux linkage in the phase coils.

With Equation 12.1 in Equation 12.2,

$$e(t) = - \frac{\partial \Psi_{PM}}{\partial x} x_m \omega_r \sin \omega_r t \quad (12.3)$$

To obtain a sinusoidal emf waveform, Equation 12.3 yields the following:

$$\frac{\partial \Psi_{PM}}{\partial x} = C_e \quad (12.4)$$

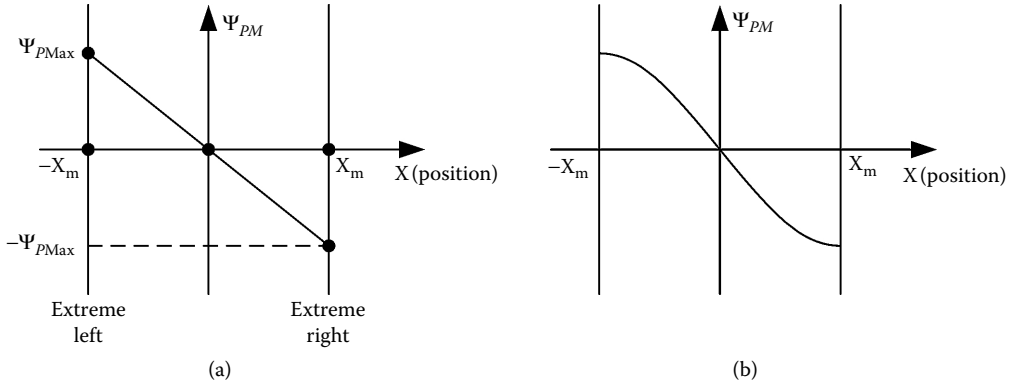


FIGURE 12.2 Permanent magnet (PM) flux linkage vs. mover position: (a) ideal and (b) real.

This means that the PM flux linkage in the phase coils has to vary linearly with mover position. Flux reversal is most adequate for the scope (Figure 12.2a and Figure 12.2b).

The excursion length is $2x_m$ but x varies sinusoidally between x_m and $-x_m$.

The ideal harmonic linear motion and PM flux linkage linear variation with mover position are met only approximately in practice.

In essence, the $\Psi_{PM}(x)$ flattens toward excursion ends (Figure 12.2b), which leads to the presence of third, fifth, and seventh harmonics in $e(t)$. Also, mainly due to magnetic saturation variation with instantaneous current and mover position, even harmonics (second and fourth) may occur in $e(t)$.

Through finite element method (FEM), the emf harmonics content for harmonic motion may be fully elucidated. The electromagnetic force $F_e(t)$ is as follows:

$$F_e(t) = \frac{e(t) \cdot i(t)}{\frac{dx}{dt}} = -\frac{\partial \Psi_{PM}}{\partial x} i(t) \quad (12.5)$$

Ideally, with $\frac{d\Psi_{PM}}{dx} = C_e$, the thrust varies as the current does.

The highest interaction electromagnetic force per given current occurs (Equation 12.5) when the $e(t)$ and $i(t)$ are in phase with each other.

For $d\Psi_{PM}/dx = \text{constant}$, from Equation 12.4, it follows that $e(t)$ is in phase with the linear speed. So, for highest thrust/current, the current has to also be in phase with the linear speed.

The above rationale is valid if the phase inductance L_s is independent of mover position, that is, if the reluctance thrust F_r is zero.

In the presence of PMs, a cogging force, F_{cog} , occurs for zero current. This force, if existent, should be zero for the mover in the middle position and maximum at excursion ends, in order to behave like a “magnetic spring” and, thus, be useful in the energy conversion ($F_{cog} \approx -K_{cog} \cdot x$) (Figure 12.3).

For the ideal LMA, with $L_s = \text{const}$, $e(t) = E_m \cos \omega_r t$, $E_m = C_e \cdot x_m \cdot \omega_r$ harmonic motion, $x = x_m \sin \omega_r t$ (provided by the regulated prime mover), and perfectly linear cogging force characteristic, under steady state, the voltage equation is, in complex variables,

$$\underline{V}_1 = -(R_s + j\omega_r L_s) \underline{I}_1 + \underline{E}_1 \quad (12.6)$$

with

$$\underline{E}_1 = E_m e^{j\omega_r t} \quad \underline{V}_1 = V_1 \sqrt{2} e^{j(\omega_r t - \delta_v)} \quad (12.7)$$

The phase voltage of the power grid $\underline{V}_1$ is phase shifted by the voltage power angle δ_v with respect to the emf $\underline{E}_1$.

The synchronization process would be similar to the case of rotary synchronous machines, but we have to regulate the motion amplitude and frequency so as to fulfill the condition of equal frequency and $E_1 = V_1$. Subsequently, to load the generator, the motion amplitude has to be increased in order to increase the delivered power.

The frequency remains constant, as the power grid is considered much stronger than the LMA. Alternatively, the stand-alone operation of LMA is not constrained in frequency, but the output is increased by increasing the motion amplitude, below the maximum limit x_{max} .

12.2.1 The Motion Equation

The equation of motion is, essentially, as follows:

$$m_t \cdot \frac{d^2 x}{dt^2} = F_{mec} - F_e + F_{cog} + F_{spring} \quad (12.11)$$

The mechanical openings force F_{spring} is

$$F_{spring} = -K \cdot x \quad (12.12)$$

with m_t equal to the total moving mass:

$$F_{cog} = -C_{cog} \cdot x \quad (12.13)$$

$$F_e(t) = C_e \cdot i(t) \quad (12.14)$$

For steady-state harmonic motion, the prime mover should ideally cover only the electromagnetic force F_e :

$$F_e(t) = F_{mec}(t) \quad (12.15)$$

Then, to fulfill Equation 12.11, we also need to know that

$$m_t \frac{d^2 x}{dt^2} + (K + C_{cog})x = 0 \quad (12.16)$$

as $x = x_m \sin \omega_r t$, it follows that

$$\left(-m_t x_m \omega_r^2 + (K + C_{cog})x_m \right) \sin \omega_r t = 0 \quad (12.17)$$

and finally,

$$\omega_r = \sqrt{\frac{(K + C_{cog})}{m_t}} \quad (12.18)$$

Equation 12.18 spells out the mechanical resonance condition. So, the electrical frequency (equal to the mechanical one) should be equal to the spring proper frequency.

In this case, the prime mover has to provide only the useful electromagnetic power, while the mechanical springs do the conversion of electrical to kinetic (and back) power at excursion ends, securing the best efficiency conditions.

If linear, the cogging spring-type force helps the mechanical springs. In reality, the cogging force drops notably at excursion ends, leading to the nonlinear spring characteristic that limits the maximum stable motion amplitude to $(0.80 \text{ to } 0.95)x_m$; x_m is the ideal maximum motion amplitude (where the flux linkage Ψ_{PM} is maximum).

Now that the basic principles are elucidated, we may proceed to the first category of LMAs — that with coil movers and stator PMs.

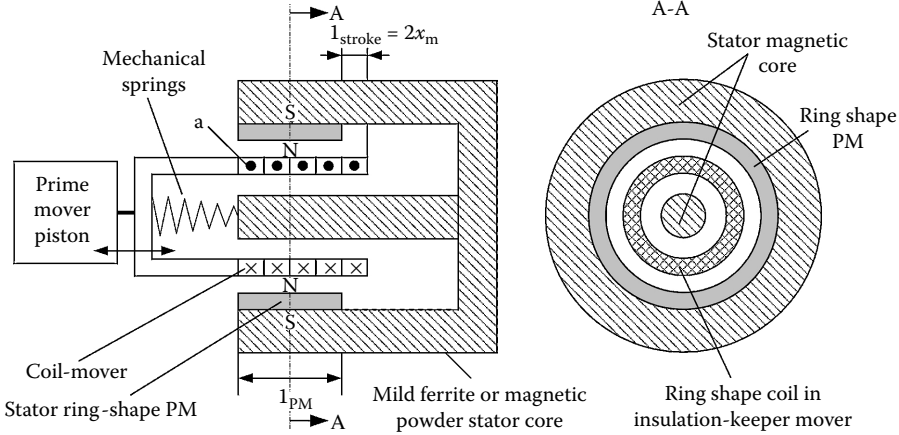


FIGURE 12.5 Homopolar linear motion alternator (LMA) with coil mover.

12.3 PM-LMA with Coil Mover

The PM-LMA with coil mover stems from the microphone (and loud speaker) principle (Figure 12.5). The ring-shaped permanent magnet is placed within a cylindrical space in the airgap. The airgap is partially filled with the coil mover. The coil mover is made of ring-shaped turns placed in an electrical insulation keeper that is mechanically rugged enough to withstand large forces. As the coil moves back and forth axially, the PM field induces an emf in the coil that is proportional to the linear speed dx/dt , the flux density B_{gPM} , the average length of the turn, and the number of active turns n'_c per PM length l_{PM} :

$$e(t) = -\frac{dx}{dt} \cdot n'_c \cdot l_{av} \cdot B_{PMav} \quad (12.19)$$

The total number of turns per coil n_c corresponds to the total coil length, which surpasses the PM length l_{PM} by the stroke length $l_{stroke} = 2x_{max}$:

$$n_c = n'_c \frac{(l_{stroke} + l_{PM})}{l_{PM}} \quad (12.20)$$

This means that only part of the coil is active in terms of emf, while the whole coil intervenes with its resistance and inductance. Alternatively, the PMs may be longer than the coil by the stroke length.

As the motion is considered harmonic, the emf $e(t)$ is sinusoidal, unless magnetic saturation on load or PM flux fringing influences B_{PMav} , making it slightly dependent on the mover position.

It should also be noticed that the homopolar character of the PM magnetic field leads to a large magnetic core of the stator, while the placement of mechanical springs is not an easy task either.

The mechanical rigidity of the coil mover with flexible electrical terminals is not high. On the good side, the mover weight tends to be low, and the copper use and heat transfer surface are large, allowing for large current densities and, thus, lower mover size, at the price of larger copper losses.

The average linear speed in LMA is rather low. For example, for speed at $f_1 = 60$ Hz and $x_m = 10$ mm, the maximum speed $U_{max} = x_m \cdot \omega_r = 0.01 \cdot 2\pi \cdot 60 = 3.76$ m/sec. The average speed $U_{av} = 4x_m f_1 = 4 \cdot 0.01 \cdot 2\pi \cdot 60$ m/sec. For given electromagnetic power, say $P_{elm} = 1200$ W, it would mean an average electromagnetic force $F_{eav} = P_{elm} / U_{av} = 1200/2.4 = 500$ N.

The total magnetic airgap h_M , which includes the two mechanical gaps g , plus the coil radial height h_{coil} , and the PM height h_{PM} is as follows:

$$h_M = h_{PM} + h_{coil} + 2g < (2 - 2.2)h_{PM} \quad (12.21)$$

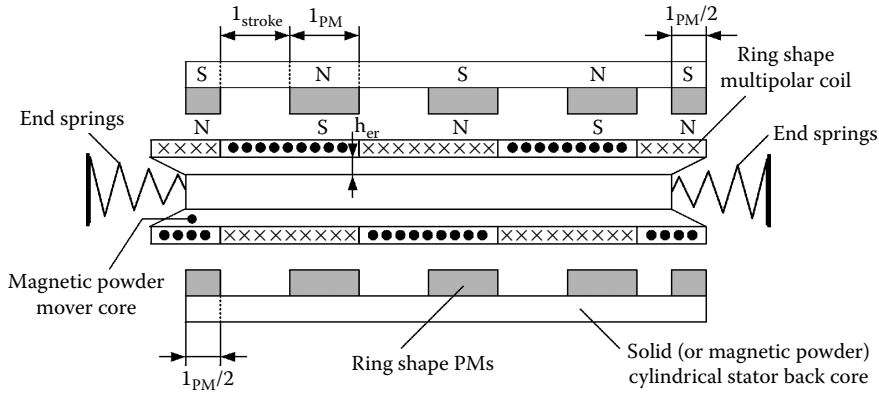


FIGURE 12.6 Heteropolar (multipolar) permanent magnet linear motion alternator (PM-LMA) with coil mover in extreme left position.

The condition in Equation 12.21 preserves large enough PM flux density levels in the airgap with a good use of PMs.

For larger powers (forces), heteropolar LMA, with multiple coils connected in counter-series or in antiparallel, may be conceived as multipolar structures (Figure 12.6). As visible in Figure 12.6, the mover now contains a magnetic core that makes it more rugged, but it adds weight. The rather small pole pitch of the PM placing makes, however, the thickness of the mover core rather small, in general, $l_{PM} \approx (1-2)l_{stroke}$. As the actual PM flux density is below $B_r/2$, and only half of the PM pole flux flows through the back core, the mover core thickness is, in general, $h_{cr} \leq l_{PM}/4$, even for $B_r = 1.2$ T. The larger the l_{PM}/l_{stroke} , the better copper utilization will be, but this comes at the price of additional iron in the back cores. An optimum situation in terms of efficiency, another in terms of costs, and yet another in terms of mover weight is felt to exist when such a coil mover LMA is designed.

Note that the increase in mover weight poses severe limitations on the frequency of stable oscillatory of motion by the prime-mover control.

As the homopolar LMA is treated in detail elsewhere [1], we will concentrate here on the multipolar (heteropolar) LMA with coil plus iron mover.

12.4 Multipole LMA with Coil Plus Iron Mover

The tubular configuration in Figure 12.5 that constitutes the multipole LMA with coil plus iron mover, is suitable for high-force applications, because as the force increases, both the mover external diameter and the number of poles $2p_1$ may be increased. This is not the case with the homopolar configuration (Figure 12.3), where only the mover diameter may be increased when more force is needed. Also, the force at zero current (cogging force F_{cog}) and the reluctance force (F_r) are both zero. The PM flux distribution and force production may be calculated with precision by two-dimensional (2D) FEM.

The stator core may be made of solid mild iron, while the mover core below the coils has to be fabricated from magnetic powders or from ring-shaped thin laminations with a filling factor of 0.95 or more to allow for large enough PM flux density in the airgap.

Magnetic saturation may be considered a second-order effect, as the coils are placed in air to reduce the mover weight and also to reduce machine inductance.

An approximate analytical model is always useful for a preliminary investigation. Let us pursue it here.

The detailed pole (section) geometry is as shown in Figure 12.7.

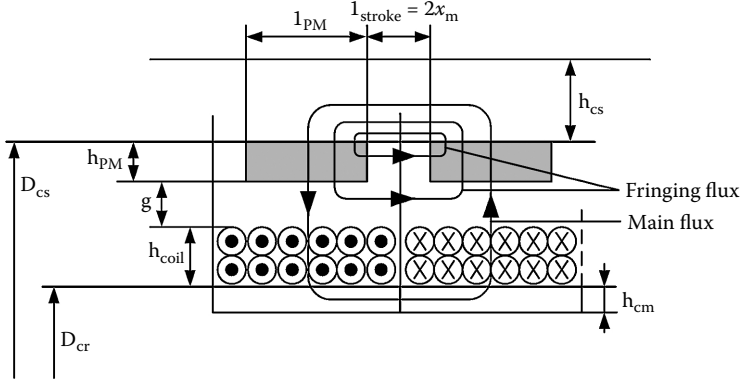


FIGURE 12.7 Geometrical details of multipole permanent magnet linear motion alternator (PM-LMA) with coil plus iron mover.

The airgap PM flux density in the airgap $B_{g^{PM}}$ is

$$B_{g^{PM}} \approx \frac{B_r \cdot h_{PM} \cdot \mu_{rec}}{h_{PM} + (g + h_{coil}) \cdot \mu_{rec}} \cdot \frac{1}{(1 + k_{fringe}) \cdot (1 + K_s)}, \quad (12.22)$$

where k_{fringe} is the fringing factor that accounts for the PM flux leaking through the airgap and between neighboring PMs, axially.

The fringing coefficient k_{fringe} depends on $(1 + (g + h_{coil})/h_{PM})$ and on l_{stroke}/h_{PM} , besides the magnetic saturation in the mover and in the stator back cores.

Though approximate analytical expressions for k_{fringe} may be derived, FEM should be used to obtain reliable information in this matter.

In general, however, for a good design, $k_{fringe} < 0.3-0.5$. K_s takes care of magnetic saturation and is generally less than 0.05 to 0.15 in a well-designed machine.

The emf in the $2p_1$ coils in series, E , is as follows:

$$E(t) = B_{g^{PM}} \cdot U(t) \cdot \pi \cdot D_{avc} \cdot 2p \cdot n_c \cdot \left(\frac{l_{PM}}{l_{PM} + l_{stroke}} \right) \quad (12.23)$$

In Equation 12.23, only the part of the coils under the direct influence of PMs is considered to produce force, while the total number of turns/coil is n_c ; D_{av} is the average coil-turn diameter.

The electromagnetic force F_e is as follows:

$$F_e(t) = B_{g^{PM}} \cdot n_c \cdot \left(\frac{l_{PM}}{l_{PM} + l_{stroke}} \right) \cdot \pi \cdot D_{avc} \cdot 2p \cdot i(t) \quad (12.24)$$

The machine inductance L_s and resistance R_s are

$$L_s \approx \frac{1}{8} \cdot 2p \cdot \mu_0 \cdot n_c^2 \cdot \pi \cdot D_{avc} \cdot \frac{l_{PM} + l_{stroke}}{(h_{PM} + g + h_{coil})} \quad (12.25)$$

$$R_s = \rho_{co} \cdot \pi \cdot D_{avc} \cdot \frac{n_c^2 \cdot 2p}{j_{con} \cdot (l_{PM} + l_{stroke})}$$

where

I_n is the RMS value of rated current

j_{con} is the rated current density in the design

For forced air cooling, $j_{con} = 6-10$ A/mm²; otherwise, it is 4 to 6 A/mm². The factor 1/8 comes from the linear variation of current-produced field with position. The core losses occur mainly in the mover core, but, at least for electrical frequencies $f_1 \leq 50(60)$ Hz, they are likely to be only a fraction (10 to 15% at most) of copper losses.

The machine behaves like a single-phase nonsalient-pole PM alternator, and thus, under steady state, the voltage equation in complex variables is as known (Equation 12.6 and Equation 12.7):

$$\underline{V}_1 = -(R_s + j\omega_1 L_s)\underline{I}_1 + \underline{E}_1 \quad (12.26)$$

The general phasor diagram in Figure 12.4 remains valid.

As the stator and mover cores handle half the pole flux, the core depths h_{cs} and h_{cr} are as follows:

$$h_{cs} \approx \frac{B_{gPM}(1 + K_{load}) \cdot I_{PM}/2}{B_{cs}}; \quad (12.27)$$

$$\frac{\pi \cdot (D_{cr}^2 - (D_{cr} - 2h_{cm})^2)}{4} \cdot B_{cr} = \frac{B_{gPM} \cdot \pi \cdot (D_{cr} - 2h_{PM})}{2} \cdot (1 + k_{load}) \times I_{PM}$$

The influence of circularity was considered in the mover, because when the mover diameter gets smaller, the latter has a notable influence.

The coefficient $K_{load} = 0.3-0.5$ accounts for the increase in flux density due to coil current for the case when the E equals in amplitude $I\omega_1 L_s$. This corresponds approximately to a power factor of $\cos\phi_1 = 0.707$ (Figure 12.4), which is considered a good compromise between force density and energy conversion performance:

$$\tan\phi_1 = \frac{I\omega_1 L_s}{E} \approx 1 \quad (12.28)$$

Example 12.1

Consider a multipolar PM-LMA with coil iron mover and the following specifications:

- Electrical output: $P_n = 22.5$ kW at $V_{1n} = 220$ V_{ac} (RMS)
- Efficiency: $\eta > 0.92$
- Load power factor: unity
- Frequency: $f_1 = 60$ Hz
- Stroke length (imposed by the prime mover): $l_{stroke} = 30$ mm
- Harmonic motion $x = \frac{l_{stroke}}{2} \sin 2\pi \cdot f_1 \cdot t$

The task is to design the machine and calculate its performance.

Solution

We shall remember that a good design requires mechanical springs attached to the mover, sized at resonance $2\pi f_1 = \sqrt{K/m_t}$, where K is the spring coefficient, and m_t is the total mover mass.

There are two ways to handle the unity power factor load: with or without a capacitor (in series with the machine phase).

In order to provide the largest stroke length for lower copper losses, not only are resonant conditions required, but also, emf $\underline{E}_1$ and current $\underline{I}_1$ should be in phase.

In this case, the power factor is mandatory lagging. Consequently, a compensating capacitor is necessary:

$$\underline{V}_1 = - \left[R_s + j \left(\omega_1 L_s - \frac{1}{\omega_1 C_s} \right) \right] I_1 + \underline{E}_1 \quad (12.29)$$

with

$$\omega_1 L_s = \frac{1}{\omega_1 C_s} \quad (12.30)$$

$$V_1 = E_1 - R_s I_1 \quad (12.31)$$

The compensating capacitor C_s makes the alternator behave like a direct current (DC) generator in the sense that only the resistive voltage drop counts. The short-circuit has to be avoided.

Once these fundamental design aspects are elucidated, we may proceed to dimensioning.

- The electromagnetic force F_{en} is

$$F_{en} = \frac{P_n}{\eta_n \cdot U_{ave}}; \quad U_{ave} = 2 \cdot l_{stroke} \cdot f_1 \quad (12.32)$$

or

$$U_{ave} = 2 \cdot 30 \times 10^{-3} \cdot 60 = 3.6 \text{ m/s}$$

$$F_{en} = \frac{22.5 \times 10^3}{0.92 \times 3.6} = 6793.5 \text{ N}$$

- The mover core external diameter D_{cr} may be obtained based on a given specific force f_{tn} , for a given number of poles $2p_1$:

$$\pi \cdot D_{cr} = \frac{F_{en}}{f_{tn} \cdot 2p_1 \cdot l_{PM}} \quad (12.33)$$

The PM pole length l_{PM} is not yet known, and a value should be adopted in relation to the stroke length l_{stroke} . Let us consider $l_{PM}/l_{stroke} = 2$, and the number of poles $2p = 4$, with $f_{tn} = 4 \text{ N/cm}^2$.

- The PM airgap flux density B_{gPM} (Equation 12.22; for neodymium–iron–boron [NdFeB] PMs with $B_r = 1.2 \text{ T}$ and $\mu_{re} = 1.07$ to 4.3) has to first be assigned an approximate value to be adjusted later, as is shown in what follows.
- Let us consider $K_{fringe} = 0.2$, $K_s = 0.05$ and $h_{PM}\mu_{rec}/(h_{PM}\mu_{rec} + g + h_{coil}) = 1/2$.
 $B_{gPM} \approx 1.2 \cdot \frac{1}{2} \cdot \frac{1}{(1+0.2)(1+0.05)} = 0.4762 \text{ T}$.

From the force equation (Equation 12.24), the ampere-turns per coil $n_c I_{av}$ is obtained ($l_{PM} = 2l_{stack} = 2 \cdot 30 = 60 \text{ mm}$):

$$F_{av} = B_{gPM} \frac{l_{PM}}{l_{PM} + l_{stack}} \cdot \pi \cdot D_{avc} \cdot 2p \cdot n_c \cdot I_{av} \quad (12.34)$$

For sinusoidal current (corresponding to harmonic motion),

$$I_{av} = I_n \sqrt{2} \cdot \frac{2}{\pi}, \quad I_n - \text{RMS value} \quad (12.35)$$

The average turn diameter is not known exactly, but if we consider D_{cr} instead of D_{avc} , the computation renders conservative results, as obtained from Equation 12.34:

$$6793.5 = 0.4762 \cdot \frac{1}{1 + \frac{1}{2}} \cdot \pi \cdot 0.2313 \cdot n_c \cdot \frac{2\sqrt{2}}{\pi} I_n$$

$n_c I_n = 8201$ Atturns per coil. (There are $2p_1 = 4$ coils.)

- These coil ampere-turns spread over a length $l_{PM} + l_{stroke} = 60 + 30 = 90$ mm. The filling factor K_{fill} for the coil, in a premade such coil, could be up to 0.6. Consequently, the height of the coil h_{coil} is

$$h_{coil} = \frac{n_c I_n}{j_{con} K_{fill} l_{PM}} = \frac{8201}{9 \cdot 10^6 \times 0.6 \times 0.06} = 38 \text{ mm} \quad (12.36)$$

- The total airgap $g = 2 \times 1.5 \text{ mm} = 3 \text{ mm}$. With $\mu_{rec} = 1.07$ from the adopted ratio $1/2$,

$$\frac{h_{PM}}{h_{PM} + (g + h_{coil})/\mu_{rec}} = \frac{1}{2} \quad (12.37)$$

we obtain $h_{PM} \approx 44 \text{ mm}$.

Both the coil height h_{coil} and the PM height h_{PM} seem to be reasonable for the force and power involved.

- To size the capacitor C_s , the machine inductance is calculated from Equation 12.25:

$$\begin{aligned} L_s &\approx \frac{1}{8} \cdot 2p_1 \cdot \mu_0 \cdot n_c^2 \cdot \pi \cdot D_{avc} \cdot \frac{(l_{PM} + l_{stroke})}{(h_{PM} + g + h_{coil})(1 + K_s)} \\ &= \frac{1}{2} 4 \times 1.256 \times 10^{-6} \cdot \pi \cdot \frac{0.2313 + 2 \cdot 0.0380.06 + 0.03}{0.044 + 0.003 + 0.038} n_c^2 = 0.55 \cdot 10^{-6} \cdot n_c^2 \text{ H} \end{aligned}$$

The stator resistance R_s (Equation 12.25) is as follows:

$$\begin{aligned} R_s &= \rho_{CO} \frac{\pi \cdot D_{avc} \cdot n_c^2 \cdot 2p_1}{\frac{l \cdot n_c}{j_{con}}} \\ &= \frac{2.3 \times 10^{-8} \times \pi \times 0.2693 \times 4 \cdot n_c^2}{\frac{8201}{9 \cdot 10^6}} = 8.5374 \times 10^{-5} \cdot n_c^2 \end{aligned} \quad (12.38)$$

The electrical time constant T_e is

$$T_e = \frac{L_s}{R_s} = \frac{0.55 \times 10^{-6} \cdot n_c^2}{1.5 \times 5.69 \times 10^{-5} \cdot n_c^2} = 0.00644 \text{ sec}$$

- The copper losses P_{con} are, simply,

$$P_{con} = R_s I_n^2 = 1.5 \cdot 5.69 \cdot 10^{-5} \cdot 8201^2 = 5.74 \text{ KW}$$

As can be seen, P_{con} are much larger than 10% of output; thus, the efficiency target is too large.

The current density j_{con} may be decreased to increase efficiency, but in this case, the size of the coil height (h_{coil}) and, consequently, the PM height have to be increased also. This is feasible for the case in point.

- There are many ways to continue this design toward optimization, based on minimum materials costs, or on $\eta \cdot \cos\phi$, etc., separately or combined.

- Here, we verify the power factor angle ϕ_1 first (Equation 12.28), with E_1 from

$$E_1 = \frac{F_{en}(U_{1\max}/\sqrt{2})}{I_n} = C_E \cdot n_c \quad (12.39)$$

$$C_e = \frac{F_{en} \cdot U_{\max}}{\sqrt{2} \cdot n_c \cdot I_n} = \frac{6793.5 \times 0.03 \cdot \pi \cdot 60}{\sqrt{2} \cdot 8201} = 3.32$$

So,

$$\tan \phi_1 = \frac{I \omega_1 L_s}{E_1} = \frac{I \cdot \omega_1 \cdot 0.55 \cdot 10^{-6} \cdot n_c^2}{3.32 \cdot n_c} = 0.512$$

and

$$\phi_1 = 27.11^\circ, \quad \cos \phi_1 = 0.89$$

This is an almost practical design value.

We may compensate the whole reactive power of L_s through a capacitor C_s :

$$C_s = \frac{1}{\omega_1^2 \cdot L_s} = \frac{10^6}{(2\pi 60)^2 \cdot 0.55 \cdot n_c^2} = \frac{12.806}{n_c^2} F \quad (12.40)$$

The reactive power in the capacitor Q_{Cs} is

$$Q_{Cs} = \frac{I_n^2}{\omega_1 C_s} = \frac{(n_c I_n)^2 2.25}{2\pi \cdot 60 \cdot 12.806} = 13.93 \text{ KVAR} \quad (12.41)$$

If the machine works in the presence of series capacitor C_s , then the voltage equation (Equation 12.31) applies:

$$V_n = 220 = -R_s I_n + E_1 = -5.69 \cdot 1.5 \cdot 10^{-5} \cdot n_c \cdot (n_c I_n) + 3.32 n_c \quad (12.42)$$

The number of turns $n_c \approx 77$ turns/coil.

The wire diameter d_{co} is

$$d_{co} = \sqrt{\frac{4 \cdot I_n \cdot n_c}{\pi \cdot j_{con} \cdot n_c}} = \sqrt{\frac{4 \times 8201}{\pi \cdot 9 \times 10^6 \times 77}} = 3.929 \times 10^{-3} \text{ m} \quad (12.43)$$

This is way too large a value; thus, quite a few conductors in parallel are required, as the current $I_n = n_c I_n / n_c = 8201/77 = 106.50 \text{ A}$.

Alternatively, a copper sheet may be used, as the conductor area required A_{co} is as follows:

$$A_{co} = \frac{I_n}{j_{con}} = \frac{106.50}{9 \times 10^6} = 11.83 \text{ mm}^2 \quad (12.44)$$

Building the coils 1.183 mm thick, may make 10 mm wide rectangular conductors feasible.

- As the power factor is large enough, it is possible to give up the capacitor. In the absence of a capacitor, the full voltage equation (Equation 12.29), with current and emf in phase and $C_s = \infty$, is to be used to calculate the number of turns per coil. A value larger than 77 will be found. At the same $n_c I_n$ and voltage V_1 , less power will be delivered, though.

Note on Performance Characteristics

Once the machine parameters are known, the voltage and force equations may be applied to calculate steady-state performance, as in any synchronous machine.

A few remarks on the coil plus iron mover PM-LMA are in order:

- As all LMAs, the coil plus iron mover LMA, with coils in an insulation keeper, is a low-speed machine for which it is advisable to work at mechanical resonance frequency; that is, electrical frequency is equal to mechanical resonance frequency.
- The coil layer provides an extra airgap; thus, the inductance of the machine is reasonably low. Consequently, the power factor is good for a specific force $f_t = 4 \text{ N/cm}^2$ in the 22.5 kW, 3.6 (average) m/sec at 60 Hz, design example.
- The reluctance and cogging forces are theoretically zero.
- The main reliability disadvantage is the presence of flexible electrical terminals to extract the electric power from the mover. As the speed is small, even copper ring brushes may be used instead.
- The copper losses are large, as each coil has to cover not only a PM pole span, but also the full stroke length. It is feasible to make the PMs longer than the coils at the price of additional PM costs.
- To make the machine more rugged, we may let the PM part move and the coils be at standstill, while putting the PM part inside the coil. But then, a PM-mover-type configuration is obtained.

12.5 PM-Mover LMAs

A few PM-mover LMAs are shown in Figure 12.8 through Figure 12.11. The configuration in Figure 12.8 contains, basically, a single ring-shaped stator coil surrounded by a U-laminated (or magnetic powder) core and a two-pole cylindrical mover with surface PMs enclosed in an insulation retainer.

The interior stator may also be made of U-shaped laminations or of magnetic powder. The magnetic powder makes the machine more manufacturable, but it essentially reduces the force, for the same PM weight and geometry, by about 30% (for $\mu_{re} = 500$ in magnetic powder).

As the mover advances from the extreme left position by half the PM pole pitch $\tau/2$, the PM flux linkage in the coil switches polarity. So, we may call the configuration a flux reversal machine [3], though it was proposed earlier than the flux reversal machines (see Reference 1).

The fragility of the mover and the leakage flux of the PM half-poles are, together with the manufacturing difficulties, the main liabilities of this otherwise moderate force density, good efficiency LMA.

The axial airgap PM-mover tubular LMA in Figure 12.9 is good for small stroke length (up to 10 mm maximum) but retains the manufacturability liabilities of the configuration in Figure 12.8. Additionally, it has a smaller usage of coils with flux linkage that switches from maximum to minimum without changing polarity.

The tubular configuration in Figure 12.10 is a typical multipole PM mover air multicore stator, with the number of coils larger than the number of PM poles by one, in general.

This type of LMA behaves basically as the one in the previous paragraph in all ways, but the placement of the air coils on the stator removes the necessity of flexible electrical terminals.

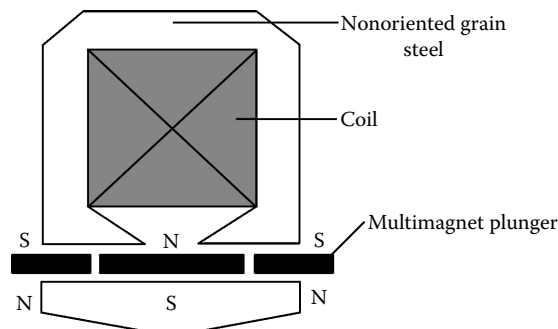


FIGURE 12.8 Linear motion alternator (LMA) with radial airgap and tubular permanent magnet (PM) mover.

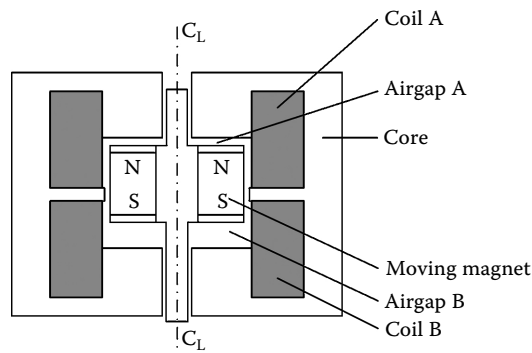


FIGURE 12.9 Linear motion alternator (LMA) with axial airgap and permanent magnet (PM) mover.

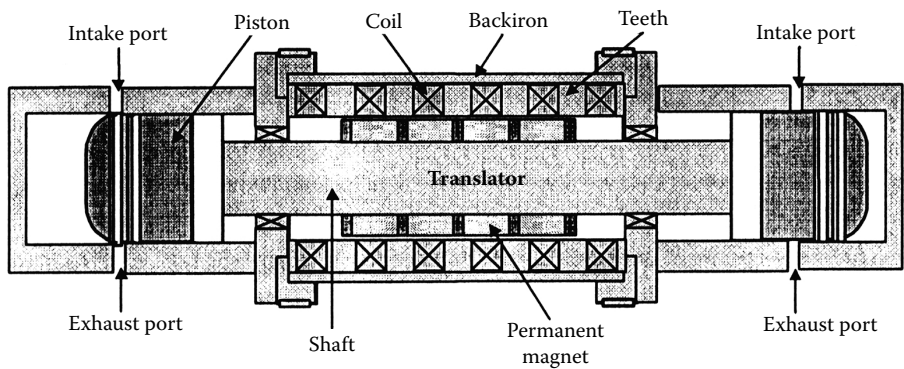


FIGURE 12.10 Linear motion alternator (LMA) with permanent magnet (PM) multipolar mover and air core stator winding.

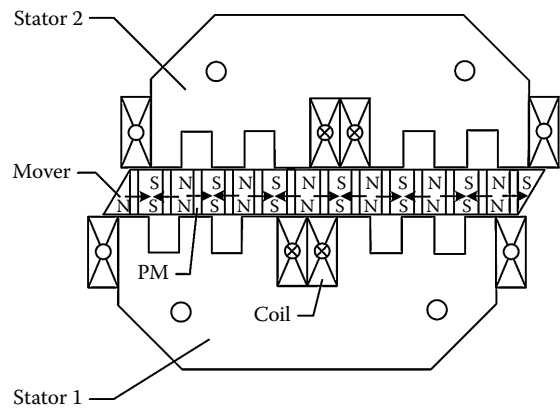


FIGURE 12.11 Flux reversal linear motion alternator (FR-LMA) with flat permanent magnet (PM) flux concentration.

All configurations above do not take advantage of PM flux concentration, but, instead, they show a reasonably small machine inductance.

In an attempt to increase the force density at the cost of larger inductance, the flat-mover PM flux concentration concept was matched with a multiple teeth (Figure 12.11) variable reluctance stator. There are two or four concentrated coils (Figure 12.11) in this machine.

The concept of flux reversal is used [4] but with a PM mover. The two stators are shifted by a PM pole pitch (or one stroke length). As the mover moves from the extreme left to the extreme right position, the PM flux linkage in the coils reverses polarity with the contribution of all PMs all the time.

Alternatively, the mover PM flux could be closed in a plane transverse to the motion direction when the concept of rotary transverse flux PM machine is applied (Figure 12.12a and Figure 12.12b), with or without PM flux concentration [5,6].

The transverse flux linear motion alternator (TF-LMA) configuration with PM flux concentration (Figure 12.12a) works as does the flux reversal linear motion alternator FR-LMA (Figure 12.11) but requires a special frame to hold the U-shaped stator cores and the stator coils. In the FR-LMA, the stator cores, premade of axial laminations and assembled in one rugged stack (which does not need spacers, etc.), make it essentially more manufacturable. At the price of additional stator core weights, though.

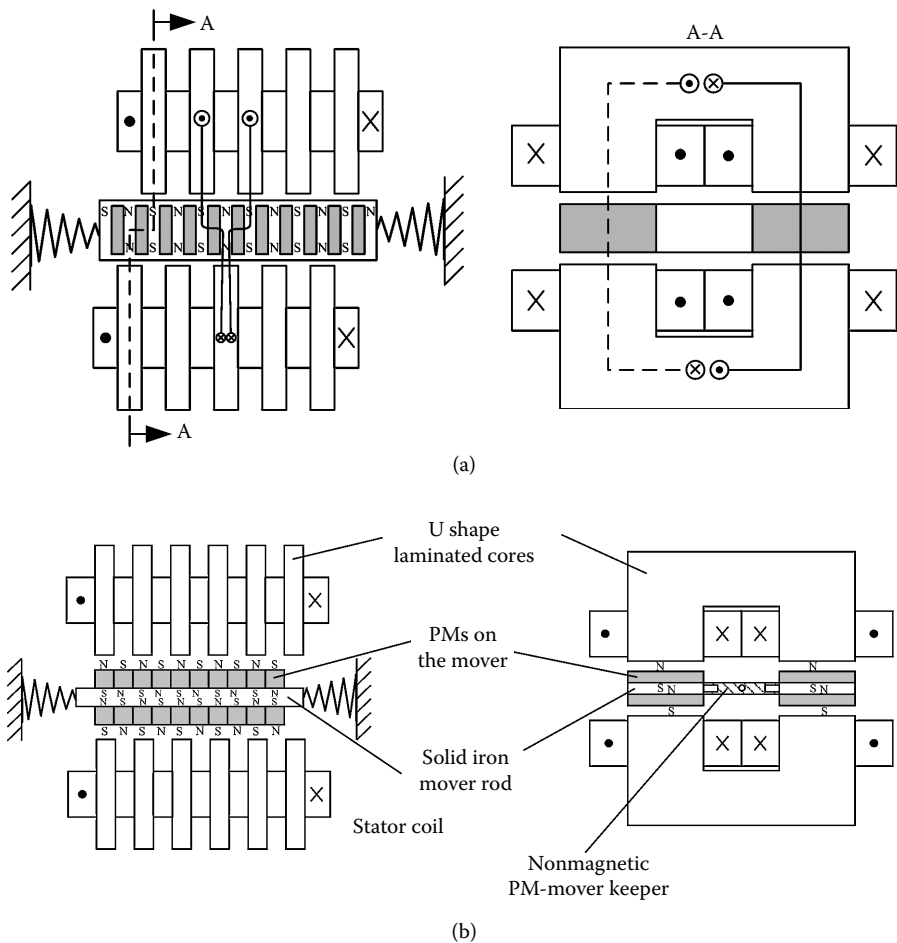


FIGURE 12.12 Generic permanent magnet (PM) mover transverse flux linear motion alternators (TF-LMAs): (a) with PM flux concentration and (b) without PM flux concentration.

In contrast, the surface PM mover TF-LMA (Figure 12.12b) makes poor usage of PMs. It has a smaller inductance that leads to a reasonable power factor, but at a lower force density than for the configuration with PM flux concentration.

Double-sided flat configurations were chosen for both TF-LMAs and FR-LMAs to secure ideal zero mover to stator total normal (attraction) force.

We will deal in the following paragraphs with the theory and design of the tubular PM mover single-coil LMA (Figure 12.8) and the FR-LMA with flux concentration (Figure 12.11).

The TF-LMA with PM flux concentration works similarly to the FR-LMA with flux concentration and, thus, is not pursued further in this chapter.

12.6 The Tubular Homopolar PM Mover Single-Coil LMA

The basic configuration for this type of LMA (Figure 12.8) has a long fragile mover with external PM half-poles that produce, through their time-variable leakage flux, eddy current losses in the machine frame. Additional frame is needed to reduce the eddy currents by placing the conducting frame parts away from the PM leakage fields. Alternatively, those fields may be magnetically contained without causing additional forces or losses.

This is why a single PM pole rotor may be preferred, at the price of halving the airgap PM flux density (Figure 12.13). A large diameter short-length aspect ratio should be aimed for, as the active length is just two stroke lengths ($2l_{stroke}$), to keep the mover mechanically rugged rigid enough. Also, a large outer stator bore diameter allows for an interior stator coil for better volume utilization.

The homopolar PM mover exhibits a dynamically unstable central position. The mechanical springs are responsible for holding the mover in the central position when the machine is turned off.

As the total length of iron in front of a PM (radially) is the same, irrespective of mover position, the cogging force is small and does not have to be considered for preliminary designs.

Also, the machine inductance is independent of mover position: $L_s = \text{constant}$.

The influence of magnetic saturation is small due to the large magnetic airgap.

Applying Ampere's and Gauss's laws, we may find the average airgap PM flux density in the airgap above the PM in the extreme right position (Figure 12.14):

$$B_{gPM} \approx B_r \cdot \frac{h_{PM}}{h_{PM} + \mu_{rec}(4g + h_{PM})} \cdot \frac{1}{(1 + K_{fringe})(1 + K_s)} \quad (12.45)$$

Thick magnets are required ($h_{PM} \gg 4g$) to provide a reasonable average PM flux density.

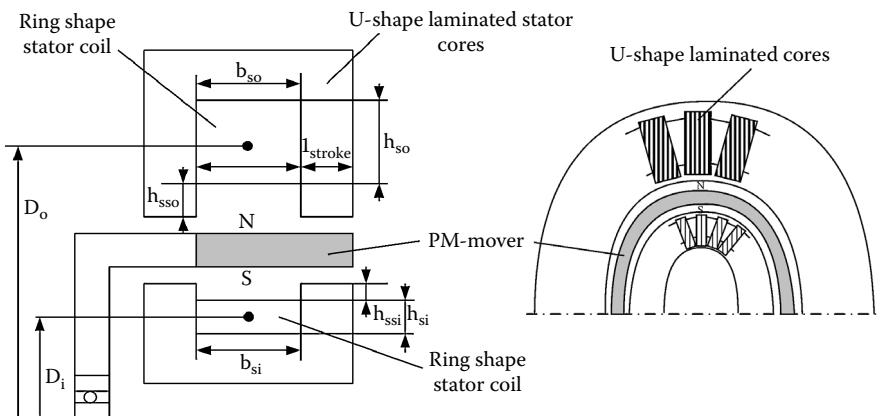


FIGURE 12.13 Unipolar permanent magnet (PM) rotor linear motion alternator (LMA) (extreme right position).

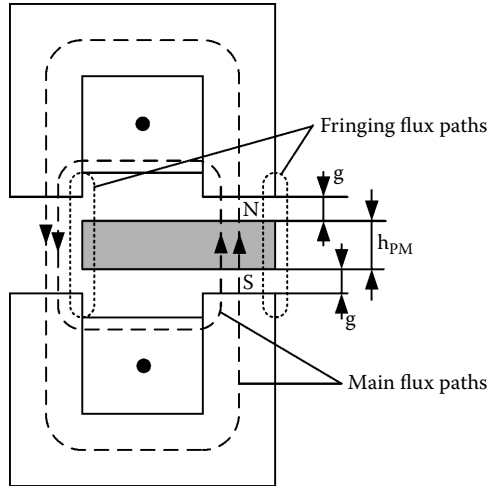


FIGURE 12.14 Permanent magnet (PM) flux paths.

Adopting harmonic motion and a linear variation of PM flux linkage in the coils from $\Psi_{PM_{Max}}$ to $-\Psi_{PM_{Max}}$, the emf varies sinusoidally in time:

$$E(t) = -2\pi f_1 B_{gPM} l_{stack} \pi D_{mav} (N_o + N_i) \cos(2\pi f_1 t) \quad (12.46)$$

where

D_{mav} is the mover average diameter

N_o, N_i are the outer and inner coil number of turns

The machine inductance has two components, L_l, L_m , corresponding to slot leakage flux and main flux path:

$$L_l = \mu_0 N_i^2 \left(\frac{h_{si}}{3b_{si}} + \frac{h_{ssi}}{b_{si}} \right) \pi D_i + \mu_0 N_o^2 \left(\frac{h_{so}}{3b_{so}} + \frac{h_{sso}}{b_{so}} \right) \pi D_o \quad (12.47)$$

$$L_m \approx \frac{\mu_0 \pi D_{mav} (N_o + N_i)^2 l_{stack}}{(1 + K_s)(4g + h_{PM}(1 + \mu_{rec}))} \quad (12.48)$$

where D_i and D_o are inner and outer coil average diameters; all the other dimensions are as shown in Figure 12.13.

For harmonic motion,

$$x = \frac{l_{stack}}{2} \sin(2\pi f_1 t) \quad (12.49)$$

the instantaneous speed $u(t)$ is

$$u(t) = \frac{dx}{dt} = \frac{l_{stack}}{2} 2\pi f_1 \cos(2\pi f_1 t) \quad (12.50)$$

For linear flux/position dependence, $E(t)$ is in counter-phase to the linear speed $u(t)$, which explains the negative sign (−) in Equation 12.46.

For sinusoidal current, instantaneous force $F_e(t)$ is as follows:

$$F_e(t) = \frac{E(t) \cdot i(t)}{u(t)} = -\frac{B_{gPM} \cdot l_{stack} \cdot \pi \cdot D_{mav} (N_o + N_i)}{\frac{l_{stack}}{2}} \cdot I \sqrt{2} \cos(2\pi f_1 t) \quad (12.51)$$

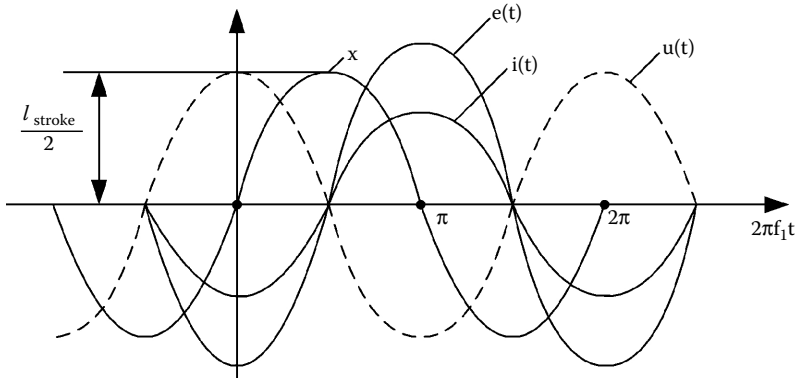


FIGURE 12.15 Position (x), speed (u), electromagnetic field (emf) (e), and current $i(t)$ for harmonic motion ideal condition (linear permanent magnet [PM] flux/position dependence).

Again, the largest force/ampere occurs for the current in phase with emf. The emf, current, speed, and position variations in time for $\gamma_1 = 0$ and harmonic motion are shown in Figure 12.15.

In reality, toward the end of excursion, the flux rise with position slows, and basically, a third harmonic occurs in the emf in addition to the fundamental.

Due to magnetic saturation, even harmonics may occur in the emf, with the force being asymmetric with respect to the middle mover position. Through FEM, such aspects could be treated with reasonable accuracy.

Note that the voltage equation and steady-state performance for various values of stroke length for autonomous or constant voltage power grid loads are as in the previous paragraph dedicated to prime coil mover PM-LMA. A numerical design example follows.

Example 12.2

Consider a homopolar PM mover LMA with outer and inner coils and the following data:

- Mean diameter of inner coil $D_i = 0.3$ m
- Stroke length $l_{stroke} = 30$ mm
- Mechanical airgap $g = 1$ mm
- PM radial thickness $h_{PM} = 10$ mm
- Outer and inner slot dimensions as follows: $b_{so} = b_{si} = 30$ mm, $h_{so} = 45$ mm, $h_{si} = 15$ mm, $h_{sso} = h_{ssi} = 3$ mm, $B_r = 1.2$ T, $\mu_{rec} = 1.05$

Calculate the following:

- The rated current for $j_{con} = 6$ A/mm² and a slot filling factor $k_{fill} = 0.5$
- The emf RMS value at $f_1 = 60$ Hz and full stroke length
- The machine inductance and resistance L_s, R_s
- For rated current and emf in phase with the current, determine the terminal voltage and active and reactive rated powers P_n, Q_n at 60 Hz

Solution

- The useful window area for the coils $A_t = (A_o + A_i)$ is as follows:

$$A_t = h_{so} b_{so} + h_{si} b_{si} = 30(45 + 15) = 1650 \text{ mm}^2$$

With a filling factor $K_{fill} = 0.5$, the total RMS magnetomotive force (mmf) $F_o + F_i$ is

$$F_o + F_i = (N_o + N_i)I_n = A_t K_{fill} j_{con} = 1650 \times 0.5 \times 6 = 4950 \text{ Atturns}$$

Finally,

$$I_n = (N_o + N_i)I_n / (N_o + N_i) = 4950 / (40 + 15) = 90 \text{ A}$$

- The mean mover diameter D_{mav} is

$$D_{mav} = D_i + h_{si} + 2h_{ssi} + 2g + h_{PM} = 300 + 15 + 2 \times 3 + 2 \times 1 + 10 = 333 \text{ mm}$$

Simply, the mean diameter of outer stator coils D_o is

$$D_o = D_{mav} + h_{so} + 2h_{sso} + 2g + h_{PM} = 333 + 2 \times 1 + 10 + 2 \times 3 + 40 = 391 \text{ mm}$$

To calculate the emf, we also need the airgap PM average flux density B_{gPM} (Equation 12.45):

$$\begin{aligned} B_{gPM} &= B_r \frac{h_{PM}}{h_{PM} + \mu_{rec}(4g + h_{PM})} \cdot \frac{1}{(1 + K_{fringe})(1 + K_s)} \\ &= 1.2 \cdot \frac{10}{10 + 1.05(4 \times 1 + 10)} \cdot \frac{1}{(1 + 0.4)(1 + 0.05)} = 0.3305 \text{ T} \end{aligned}$$

The fringing factor $K_{fringe} = 0.4$ and the magnetic saturation factor $K_s = 0.05$ are conservative values in the equation above.

It should be noticed that the flux density is rather small, despite the use of good PMs ($B_r = 1.2 \text{ T}$). Fringing plays an important role, together with the homopolar magnet configuration, in this notable reduction in performance.

The emf is now as follows (Equation 12.46):

$$\begin{aligned} E(\text{rms value}) &= \frac{2\pi f_1}{\sqrt{2}} B_{gPM} \cdot l_{stroke} \cdot \pi \cdot D_{mav} \cdot (N_o + N_i) \\ &= \frac{2\pi \cdot 60}{\sqrt{2}} \times 0.3305 \times 0.03 \times \pi \times 0.333 \times (40 + 15) = 152.376 \text{ V} \end{aligned}$$

- The machine inductance components L_m , L_l are straightforward (Equation 12.47 and Equation 12.48):

$$L_m = \frac{\mu_0 \cdot \pi \cdot D_{mav} \cdot (N_o + N_i)^2 \cdot l_{stroke}}{(1 + K_s) \cdot (4g + h_{PM}(1 + \mu_{rec}))} = \frac{1.256 \times 10^{-6} \cdot \pi \cdot 0.333 \times (40 + 15)^2 \times 0.03}{(1 + 0.05) \cdot (4 \times 1 + 10(1 + 1.05))10^{-3}} = 4.633 \times 10^{-3} \text{ H}$$

$$\begin{aligned} L_l &= \mu_0 N_i^2 \left(\frac{h_{si}}{3b_{si}} + \frac{h_{ssi}}{b_{si}} \right) \pi D_i + \mu_0 N_o^2 \left(\frac{h_{so}}{3b_{so}} + \frac{h_{sso}}{b_{so}} \right) \pi D_o \\ &= 1.25610^{-6} \left[40^2 \left(\frac{40}{3 \cdot 30} \cdot \frac{3}{30} \right) \times 0.333 + 15^2 \left(\frac{15}{3 \cdot 30} \cdot \frac{3}{30} \right) \times 0.391 \right] = 1.229 \times 10^{-3} \text{ H} \end{aligned}$$

$$L_s = L_m + L_l = (4.633 + 1.229)10^{-3} = 5.8626 \times 10^{-3} \text{ H}$$

The total resistance R_s is

$$R_s = \rho_{co} \frac{(\pi D_i N_i + \pi D_o N_o) I_n}{I_n} j_{con} = 2.3 \cdot 10^{-8} \pi \cdot (0.333 \times 15 + 0.381 \times 40) \times 6 \times 10^6 = 0.09742 \text{ } \Omega$$

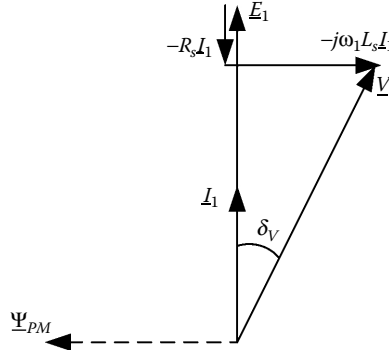


FIGURE 12.16 Phasor diagram for $\underline{E}_1$, $\underline{I}_1$ in phase.

The rated copper losses $P_{con} = R_s I_n^2 = 0.09742 \times 90^2 = 780 \text{ W}$.

- To calculate the terminal voltage, the phasor diagram is required (Figure 12.16):

$$\underline{V}_1 = -\underline{I}_1(R_s + j\omega_1 L_s) + \underline{E}_1$$

From Figure 12.16,

$$-\varphi_1 = -\delta_v = \tan^{-1} \left(\frac{\omega_1 L_s I_1}{E_1 - R_s I_1} \right) = \tan^{-1} \left(\frac{2\pi \cdot 60 \times 5 \cdot 8620 \times 10^{-3} \cdot 90}{152.376 - 1.09742 \cdot 90} \right) = \tan^{-1}(1.384) = 54.15^\circ$$

$$\cos \varphi_1 = 0.5856$$

This is a moderately low power factor. The machine is absorbing considerable reactive power.

The terminal voltage V_1 is, simply,

$$V_1 = \frac{(E_1 - R_s I_n)}{\cos \varphi_1} = \frac{152.376 - 0.09742 \cdot 90}{0.5856} \approx 245.2 \text{ V}$$

The delivered active power P_n is

$$P_n = V_1 I_n \cos \varphi_1 = 245.2 \times 90 \times 0.5856 = 12.924 \text{ kW}$$

The copper losses are about 6% of rated output power.

The absorbed reactive power Q_n is

$$Q_n = V_1 I_n \sin \varphi_1 = 245.2 \times 90 \times \sin(-54.15^\circ) = -17.88 \text{ kVAR}$$

A 17.88 kVAR series capacitor has to be added to compensate for this reactive power and thus make the LMA work with resistive voltage regulation.

12.7 The Flux Reversal LMA with Mover PM Flux Concentration

The flat double-sided configuration in Figure 12.11 represents the flux reversal LMA with mover PM flux concentration (FR-LMA-FC). It is reproduced here in its cross-sectional form to assist in performance computation (Figure 12.17).

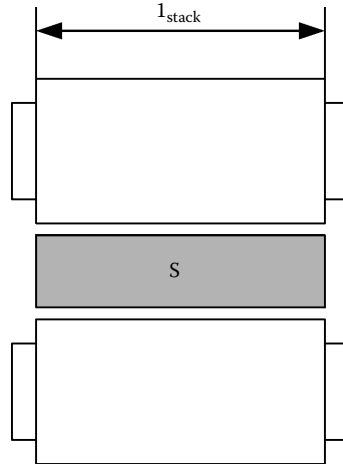


FIGURE 12.17 The flux reversal linear motion alternator (FR-LMA) with mover permanent magnet (PM) flux concentration (see also Figure 12.14).

It should be noted first that the two identical stators are shifted by the maximum stroke length: l_{stroke} . The maximum stroke length is equal to PM pitch τ_{PM} and to half the small flat pitch τ_s in the stator poles: $\tau_{PM} = \tau_s/2 = l_{stroke}$.

To make the best use of copper in the coils, the coil turn geometry should be close to a quadrangle (which is closest to a circle). The PM flux linkage in the coils switches polarity from the extreme left position to the extreme right position by l_{stroke} .

All PMs are active all the time, but again, flux fringing is a severe limiting factor in performance.

The mechanical airgap should be small, to reduce fringing, but not too small, as the machine inductances increase too much. In principle, the machine inductance varies with mover position.

PM flux concentration is obtained as the h_{PM}/l_{stroke} ratio becomes greater than unity.

To overcome the natural increase in inductance, which is maximum in axis q (when the PM flux linkage in the coils is zero; Figure 12.15), the flux concentration has to be substantial, as it also has to cover for large fringing.

Example 12.3

Let us consider an FR-LMA-FC with the following specifications:

- $P_n = 1$ kW
- $\eta_n \approx 0.93$
- $U_n = 120$ V
- $f_1 = 60$ Hz
- Stroke length: $l_{stroke} = 12$ mm

The task is to do preliminary sizing of such a machine.

Solution

First, we have to calculate the average airgap PM flux density under stator tooth for the mover position of maximum flux linkage in the stator coils:

$$B_{gPM} \tau_{PM} = \frac{2B_m l_{PM}}{(1+K_{fringe})} 2g \frac{B_{gPM}}{\mu_0} + H_m h_{PM} = 0 \quad B_m = B_r + H_m \mu_{rec} \quad (12.52)$$

From Equation 12.52, after B_m and H_m are eliminated,

$$B_{gPM} = \frac{Br \cdot 2l_{PM}/\tau_{PM}}{(1 + K_{fringe}) \left(1 + \frac{\mu_{rec}}{\mu_0} \cdot \frac{4g l_{PM}}{\tau_{PM} h_{PM}} \right)} \quad (12.53)$$

with $2l_{PM}/\tau_{PM} = 5$, $K_{fringe} = 0.5$, $g = 10^{-3}$ m, $h_{PM} = 4 \times 10^{-3}$:

$$B_{gPM} = \frac{1.2 \times 5}{(1 + 0.5) \left(1 + \frac{1.05 \times 4 \times 10^{-3} \times 2.5}{4 \times 10^{-3}} \right)} = 1.103 \text{ T}$$

The maximum flux in a coil Φ_{max} is as follows:

$$\Phi_{max} = 2.5 \cdot B_{gPM} \cdot l_{stack} \cdot \tau_{PM} = 2.5 \times 1.103 \times l_{stack} \times 0.012 = 0.033 \cdot l_{stack}$$

The average force F_e is

$$F_e = \frac{P_n}{\eta_n \cdot U_{av}} = \frac{2000}{0.93 \times 2 \times 0.012 \times 60} = 746.7 \text{ N} \quad (12.54)$$

Also, the total force has the following expression:

$$F_e = 4 \cdot \frac{2}{\tau_{PM}} \cdot \Phi_{PMax} \cdot \frac{1}{\sqrt{2}} \cdot (n_c I_n) \quad (12.55)$$

The stack length l_{stack} has to be determined based on the force density concept ($f_m = 4$ N/cm² in our case):

$$F_e = f_m \cdot 4l_{stack} \cdot 5\tau_{PM} \quad (12.56)$$

$$l_{stack} = \frac{746.7}{4 \cdot 4 \times 10^4 \times 5 \times 0.012} = 0.078 \text{ m}$$

Fortunately, l_{stack} is close to 5 = 60 mm.

From Equation 12.55, the ampere-turns per coil $n_c I_n$ can be calculated:

$$n_c I_n = \frac{F_e \cdot \tau_{PM} \cdot \sqrt{2}}{2 \times 4 \times 0.033 \cdot l_{stack}} = \frac{746.7 \times 0.012 \times \sqrt{2}}{0.264 \times 0.078} = 613.54 \text{ Ampere-turns/coil} \quad (12.57)$$

For a large slot filling factor, $K_{fill} = 0.55$ (premade coils), the window area A_w is as follows:

$$A_w = \frac{2n_c \cdot I_n}{j_{con} \cdot K_{fill}} = \frac{2 \times 613.54}{6 \times 10^6 \times 0.55} = 371.8 \text{ mm}^2 \quad (12.58)$$

With the window width $W_s = 2\tau_{PM} = 24$ mm, the large slot height $h_s = A_w/W_s = 371.8/24 = 15.49$ mm. This small value is good for reducing the slot leakage inductance.

The emf is computable from the following:

$$E = \frac{F_e \cdot U_{av}}{I_n} = K_e \cdot n_c \quad (12.59)$$

or

$$K_e = \frac{746.7 \times 1.44}{613.54} = 1.752$$

Further on, the machine inductance L_s is obtained from its two components L_m and L_l :

$$\begin{aligned} L_m &\approx 4\mu_0 \cdot n_c^2 \times \frac{3\tau_{PM} l_{stack}}{g} \cdot (1 + K_{fringe}) \times \frac{(\tau_{PM} - h_{PM})}{\tau_{PM}} \\ &= 4 \cdot 1.256 \times 10^{-6} \times \frac{3 \times 0.012 \times 0.078}{1 \times 10^{-3}} (1 + 0.5) \times n_c^2 \times \frac{2}{3} = 1.3966 \times 10^{-5} n_c^2 \end{aligned} \quad (12.60)$$

$$L_l \approx 4\mu_0 n_c^2 \left[l_{stack} \frac{h_s}{3W_s} + l_{end} \cdot 0.33 \right] \quad (12.61)$$

The turn end connection, length, l_{end} , is as follows:

$$l_{end} \approx l_{stack} + 10\tau_{PM} + 3W_s/2 = 0.078 + 10 \times 0.012 + 3 \times 0.024/2 = 0.234 \text{ m}$$

The factor 0.33 represents the usual approximation used for single coils in induction machinery:

$$L_l = 4 \times 1.256 \times 10^{-6} \left[0.078 \times \frac{15.49}{3 \times 24} + 0.27 \times 0.33 \right] n_c^2 = 0.532 \times 10^{-6} n_c^2$$

The total resistance R_s with all coils in series R_s is as follows:

$$R_s = 4\rho_{co} \frac{l_{coil} \cdot n_c^2}{\frac{I_n n_c}{J_{con}}} = \frac{4 \cdot 2.3 \times 10^{-8} \cdot 0.324 \cdot n_c^2 \times 6 \times 10^6}{613.54} = 2.915 \times 10^{-4} n_c^2$$

The coil length l_{coil} is

$$l_{coil} \approx 2l_{stack} + 10\tau_{PM} + 4 \times \frac{W_s}{2} = 2 \times 0.078 + 10 \times 0.012 + 4 \times \frac{0.024}{2} \approx 0.324 \text{ m}$$

Let us consider that a series capacitor fully compensates the inductance L_s and, thus,

$$V_1 = -R_s \cdot I_n + E_1$$

$$120 = -2.915 \cdot 10^{-4} \cdot n_c \cdot (n_c \cdot I_n) + 1.752 \cdot n_c$$

$$\text{With } n_c \cdot I_n = 613.54 \text{ Aturns}$$

$$n_c = 76 \text{ turns/coil}$$

Finally, $R_s = 1.6837 \Omega$.

The rated current $I_n = \frac{n_c \cdot I_n}{n_c} = \frac{613.54}{76} = 8.0723 \text{ A}$.

The machine maximum inductance, on axis q , is

$$L_s = L_m + L_l = (1.3966 \times 10^{-5} + 0.532 \times 10^{-6}) \times 76^2 = 0.08263 \text{ H}$$

The copper losses P_{con} are as follows:

$$P_{con} = R_s \cdot I_n^2 = 1.6537 \times 8.0723^2 = 109.7 \text{ W}$$

It is now visible that, with this design, the efficiency target of 0.93 may not be reached, as the copper losses are already 10% of the output of about 1 kW: $P_n = I_n \cdot V_n = 8.073 \cdot 120 \approx 968 \text{ W}$. Reducing the current density will result in increased efficiency.

It is important now to calculate the power factor angle ϕ_1 in the machine for rated output:

$$\phi_1 \approx \tan^{-1} \frac{(I_n \omega_1 L_s)}{E_1} = \tan^{-1} \frac{(8.0723 \cdot 2\pi \cdot 60 \cdot 0.08203)}{1.752 \times 76} = \tan^{-1}(1.88) = 62^\circ; \quad \cos \phi_1 \approx 0.47$$

Note that the power factor is rather poor, as expected. So, we will need a sizable capacitor to compensate for it:

$$Q_c = Q_{Ls} \approx \omega_1 \cdot L_s \cdot I_n^2 = 2\pi \cdot 60 \times 0.08263 \times 8.0723^2 = 2.029 \text{ kVAR(!)}$$

However, the machine size for 1 kW is acceptable with a total weight of less than 8 kg. What made it possible was heavy PM flux concentration with full use of PMs all the time.

FEM Analysis

For a similar prototype but with a 100 mm stack length, a detailed FEM analysis was performed [4].

The flux paths for the extreme right and middle positions are shown in [Figure 12.17](#).

It is apparent that the flux is not quite zero in the middle position, as ideally it should be ([Figure 12.18a](#) and [Figure 12.18b](#)).

The static forces vs. position for constant DC ampere-turns per coils is shown in [Figure 12.19](#).

It is clear that the force decreases toward the excursion ends and is not fully symmetrical with respect to the middle position. Then, when the current is sinusoidal and in antiphase with the speed (in phase with the emf), the force vs. position looks as shown in [Figure 12.20](#).

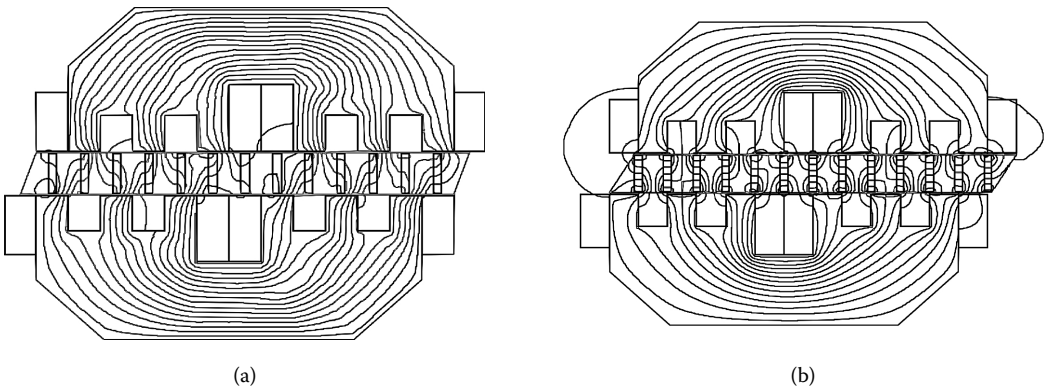


FIGURE 12.18 Flux paths (at zero current) for (a) extreme right and (b) middle position.

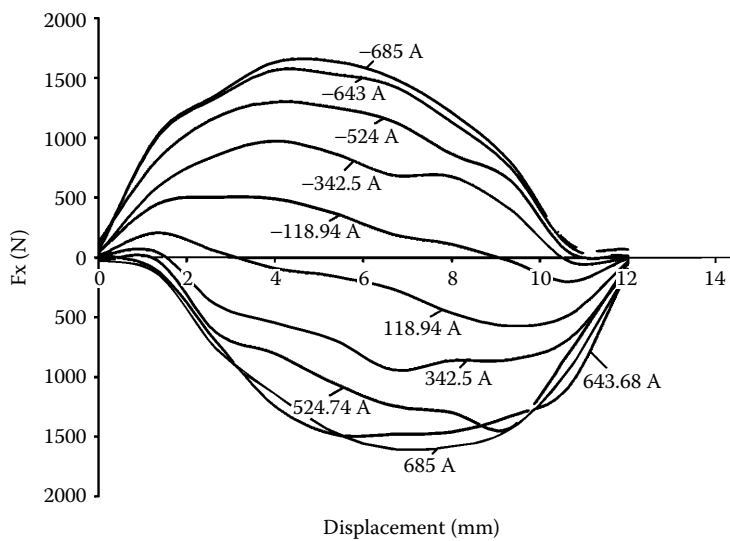


FIGURE 12.19 Static force vs. position for constant coil magnetomotive force (mmf) (RMS).

The presence of reluctance force due to mild magnetic saliency is evident in Figure 12.19, and it also appears in Figure 12.20. Figure 12.21 illustrates inductance vs. position (for one turn per coil) and rated current.

The PM airgap flux density distribution for the extreme left position of the mover is apparent in Figure 12.22. It is clearly visible that the average design $B_{gPM} \approx 1.1$ T under the stator teeth is actually obtained. So, the analytical predictions were realistic ($K_{fringe} = 0.5$).

Finally, the peak cogging force in Figure 12.23 of about 270 N, is about 35% of the average electro-magnetic force of 746 N. It is not zero exactly in the middle position, but it is monotonous (as in a spring) over almost 11 mm out of the 12 mm maximum stroke length. With strong springs to cover for this cogging force nonlinearity toward stroke ends, the operation is expected to be stable over the maximum stroke length of $\tau_{PM} = 12$ mm. The cogging force saves some of the otherwise larger required mechanical spring material.

It may be stated that the above analytical and FEM investigations corroborate well enough to be a solid base for refined designs, optimization, transients, and control.

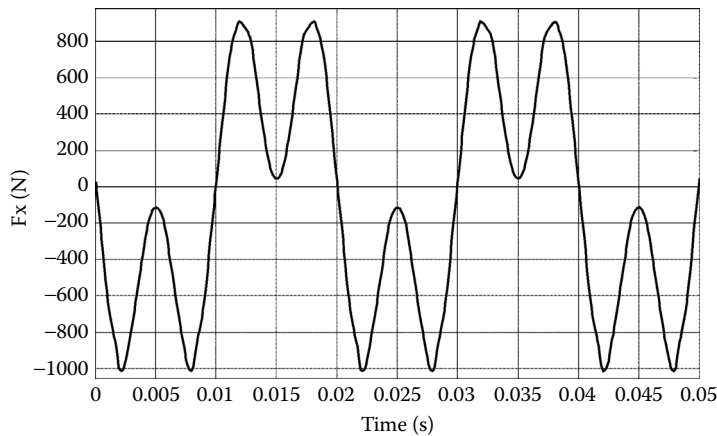


FIGURE 12.20 Thrust vs. position for sinusoidal current.

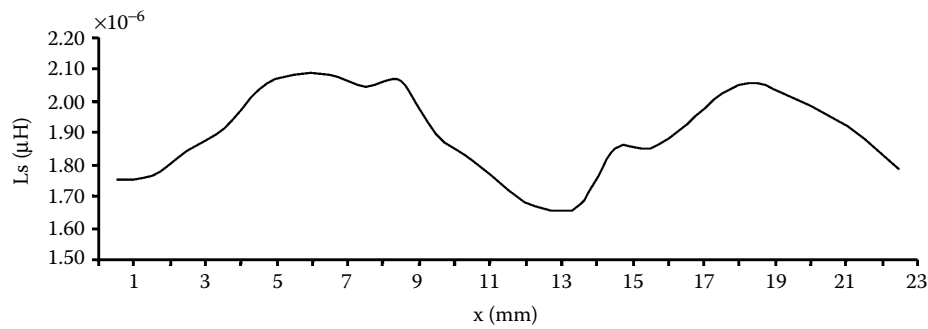


FIGURE 12.21 Inductance vs. position (for one turn per coil) and rated current.

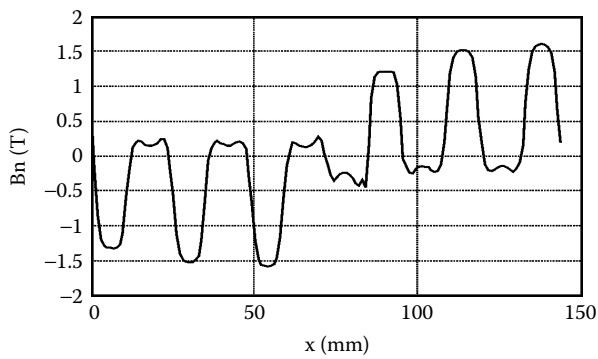


FIGURE 12.22 Permanent magnet (PM) airgap flux density distribution at extreme left position of the mover.

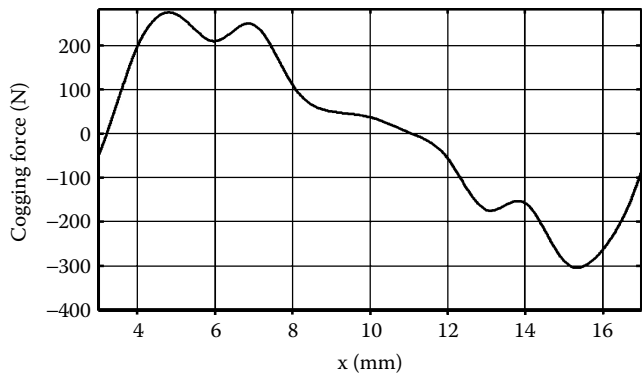


FIGURE 12.23 The cogging force.

12.8 PM-LMAs with Iron Mover

A few PM-LMAs with iron mover were proposed (Figure 12.24a through Figure 12.24f). They may be classified into flux switch (Figure 12.24a through Figure 12.24c) and flux reversal (Figure 12.24d) configurations.

While configurations in Figure 12.24a through Figure 12.24d are tubular, the ones in Figure 12.24e and Figure 12.24f are flat and double sided.

It should be noted that the tubular configuration in Figure 12.24d [3] and the flat ones in Figure 12.24e and Figure 12.24f [6] are based on the same, flux reversal, principle.

As both have about the same merits and demerits we will treat here in some detail the tubular version, as it seems to be more manufacturable, and it also offers more flexibility in design (the number of radial poles may be $2p_1 = 4, 6, 8$). The lamination design does not depend on the stroke length because the flux paths are essentially in the radial plane, which is transverse to the motion direction. Another stator PM flux concentration configuration is introduced in Figure 12.24f.

12.9 The Flux Reversal PM-LMA Tubular Configuration

The tubular configuration of the flux reversal PM-LMA (Figure 12.24d) takes advantage of the basic topology of the switched reluctance machine.

It conventionally uses stamped radial laminations with $2p_1$ radial poles. Along the axial direction, $2n$ PMs are attached to the stator poles with alternate polarity and a pitch of τ_{PM} .

The mover is also made of stamped laminations, but axially, only each other pole is filled with laminations with the interpole made of an insulating low-density material, to reduce mover weight.

The number of radial poles $2p_1$, the number of axial poles $2n$, the stator bare diameter D_{is} , and the PM thickness h_{PM} are the main variables to consider in a design, in addition to the PM pole pitch τ_{PM} , equal to the stroke length ($l_{stroke} = \tau_{PM}$). Then other variables, such as the stator pole angle $\alpha_p = (0.5 \text{ to } 0.6)\pi/p_1$, the coil height h_{coil} , and the stator back core radial height h_{coil} come into play (Figure 12.25).

The blessing of circularity is no small advantage in securing an ideally zero radial force for zero eccentricity and in facilitating easy framing.

The PM flux in the stator $2p_1$ coils reverses polarity (when the mover moves by l_{stroke} from the extreme left to the extreme right position) from $+\Phi_{PM}$ to $-\Phi_{PM}$ per one radial $\times$ one axial pole lengths.

On the other hand, the PM flux of axial PM pole varies from $\Phi_{PM_{Max}}$ to $\Phi_{PM_{Min}}$, and thus,

$$\Phi_{PM} = \Phi_{PM_{Max}} - \Phi_{PM_{Min}} \quad (12.62)$$

How to maximize Φ_{PM} for given D_{is} , $2p_1$, and l_{stroke} is a complex problem. However, it was realized [1] that when the mover laminated pole length is $l_m = \tau_{PM}/2$ and $(g + h_{PM})/\tau_{PM} = 0.5$, the maximum force density f_t (N/cm²) is obtained. For minimum copper losses per N of force, however, $l_m/\tau_{PM} = 1$ (pole/interpole).

Though much of PM flux lines flow in the radial plane, due to alternate polarity along the axial direction, some of them flow in the axial plane as fringing flux. This effect has to be calculated in a three-dimensional (3D) FEM or quasi-2D FEM [7].

An analytical model is developed first, and then test results on a prototype are shown.

12.9.1 The Analytical Model

First, the maximum and minimum flux per axial pole $\Phi_{PM_{Max}}$ may be written as follows:

$$\begin{aligned} \Phi_{PM_{Max}} &= \frac{G_{Max} I_{PM}}{1 + K_s}; & I_{PM} &= H_c \cdot h_{PM} \\ \Phi_{PM_{Min}} &= \frac{G_{Min} I_{PM}}{1 + K_s} \end{aligned} \quad (12.63)$$

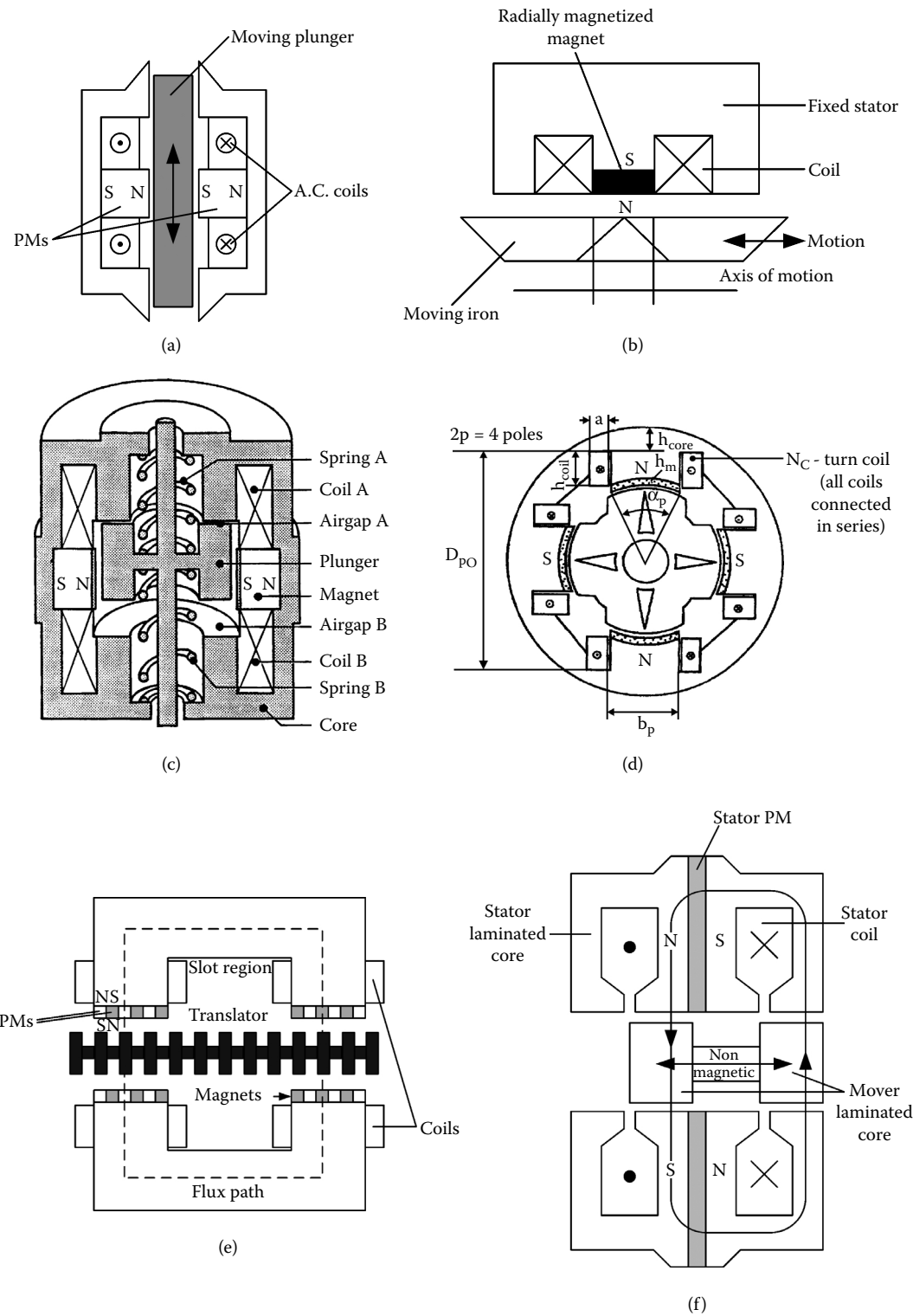


FIGURE 12.24 Permanent magnet linear motion accelerators (PM-LMAs) with iron movers: (a) with radial airgap, (b) with pulsed flux, (c) with axial airgap, (d) tubular with flux reversal, (e) flat with flux reversal, and (f) flat with PM flux stator concentration.

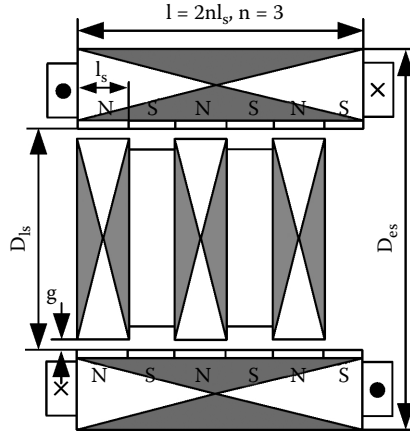


FIGURE 12.25 Main geometry variables in a $2p_1 = 4$ radial and $2n = 6$ axial poles.

The magnetic permeances $G_{\max}$ and $G_{\min}$ are as follows [3]:

$$\begin{aligned}
 G_{\max} &= \mu_0 \pi \left(D_{is} - 2g + \frac{(g + h_{PM})}{2} \right) \cdot \frac{l_{stroke}}{g + h_{PM}} \cdot \frac{\alpha_p}{360} \\
 G_{\min} &= G_{\min 1} + G_{\min 2} \\
 G_{\min 1} &\approx 3.3 \mu_0 \frac{1}{2} (D_{is} - g + h_{PM}) \cdot \frac{\alpha_p}{360} \\
 G_{\min 2} &\approx 4 \mu_0 \left(\frac{1}{2} D_{is} - \sqrt{\frac{l_{stroke} (g + h_{PM})}{2}} \right) \cdot \left(\ln \left(\frac{l_{stroke}}{2(g + h_{PM})} \right) \right) \cdot \frac{\alpha_p}{360}
 \end{aligned} \tag{12.64}$$

If $l_{stroke} \leq 2(g + h_{PM})$, then $G_{\min 2} = 0$. $G_{\min}$ accounts approximately for flux axial fringing.

The emf, $e(t)$, is now, simply,

$$e(t) = \frac{d\Phi_{PM}}{dx} \cdot N \cdot 2p_1 n \cdot \frac{dx}{dt} \approx \frac{2\Phi_{PM}}{l_{stroke}} \cdot 2pn \cdot N \cdot u(t) \tag{12.65}$$

The machine inductance, independent of mover position, L_s is

$$\begin{aligned}
 L_s &= L_m + L_l \\
 L_m &= C_m \cdot N^2; \quad C_m = 4p_1 n (G_{\max} + G_{\min})
 \end{aligned} \tag{12.66}$$

The leakage inductance L_l is

$$\begin{aligned}
 L_l &= C_l \cdot N^2 \\
 C_l &= 4p_1 \mu_0 (\lambda_s l_{cs} + \lambda_{end} l_{end})
 \end{aligned} \tag{12.67}$$

where

$$\begin{aligned}
 l_{cs} &= 2n \cdot l_{stroke} \\
 l_{end} &= b_p + \pi a/2
 \end{aligned}$$

b_p is the stator pole width in the radial plane

a is the coil width in radial plane

l_{cs} is the coil in slot length

l_{end} is the coil end connection length

The slot and end connection geometrical permeance coefficients λ_s and λ_{end} are as follows:

$$\lambda_s \approx \frac{h_{coil}}{3a}; \quad \lambda_{end} \approx \lambda_s/2; \quad h_{coil} - \text{coil height} \quad (12.68)$$

Finally, the phase resistance with all coils in series, R_s , is

$$R_s = 2p_1 \cdot \rho_{co} \cdot \frac{l_{coil} \cdot N^2}{\frac{I_n \cdot N}{j_{con}}}; \quad l_{coil} = (2n \cdot l_{stroke} + b_p + \pi a/2) \times 2 \quad (12.69)$$

where

I_n is the rated RMS current

j_{con} is the design current density

N is the turns per coil

The instantaneous force F_e is written, with Equation 12.65, as follows:

$$F_e = \frac{e(t) \cdot i(t)}{u(t)} = \frac{2\Phi_{PM}}{l_{stroke}} \cdot 2p_1 n \cdot N \cdot i(t) = C_F \cdot i(t) \quad (12.70)$$

The above model is based on the assumption that the PM flux in the stator coil (per axial–radial PM pole) Φ_{PM} varies linearly with mover position. In reality, at least a third harmonic may be detected in the emf once the excursion length comes close to the maximum (ideal) stroke length. Consequently, the force F_e will not be strictly proportional to current (C_F will decrease with the mover on departure from the middle position).

Proof of these phenomena is shown, through tests, on a 125 W, 120 V, 60 Hz linear alternator with $l_s = 10$ mm [3], in Figure 12.26 and Figure 12.27.

For the prototype, the measured cogging F_{cog} (at zero current), electromagnetic force F_e (current only), and total force F_t are shown in Figure 12.28. It is to be noted that, for constant current, the electromagnetic force F_e decreases notably toward the excursion end. The cogging force is large but it increases steadily up to more than 90% of full stroke length ($2 \times 5 = 10$ mm).

Note again that the cogging force behaves like an additional mechanical spring. The actual springs should only complement the cogging force to secure stable oscillations up to ideal full stroke length. It should also

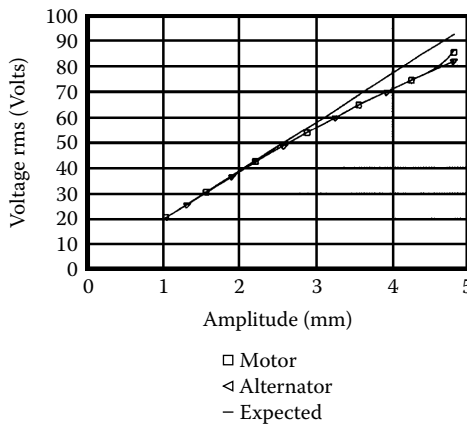


FIGURE 12.26 Electromagnetic force (emf) vs. mover position for $l_{stroke} = 10$ mm (test results).

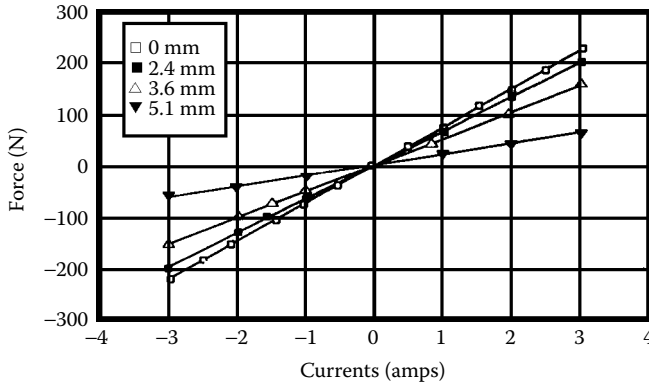


FIGURE 12.27 Current-only force vs. current for various motion amplitude $x_m = l_{stroke}/2$ (test results).

be borne in mind that toward the stroke end, the current in the coil decreases to zero, and thus, the corresponding decrease of the force constant C_F does not produce severe damage in performance (in average force).

A capacitor is added to compensate (partially or totally) for the machine inductance voltage drop.

For a resistive passive load, the measured terminal voltage vs. current is shown in Figure 12.29 for three values of the capacitor, all below full compensation conditions.

Electrical efficiencies above 85% were measured in back-to-back motor/generator loading experiments of the 125 W prototype.

Note that a design example for such a 1 kW alternator is given in Reference 1 and, thus, is not repeated here.

We investigated a number of practical configurations for PM-LMAs and conclude the following:

- All operate as single-phase synchronous alternators.
- All should operate so that electrical and mechanical resonance and frequency are equal: $f_e = \sqrt{K_t/m_t} / 2\pi$.
- For all, in general, mechanical springs, made of copper–beryllium flexures [4], are used to secure stable oscillations up to full stroke length.
- For all, to decrease the electrical power delivered on a given load resistance, the stroke length is generally reduced by adequate control in the prime mover, while $f_e = \text{constant}$, and so is the terminal voltage.

In what follows, we will dwell a while on the control and stability of PM-LMAs.

The phase coordinate state-space model of PM-LMA is straightforward, and thus, it is not developed here though is needed for transients and control design.

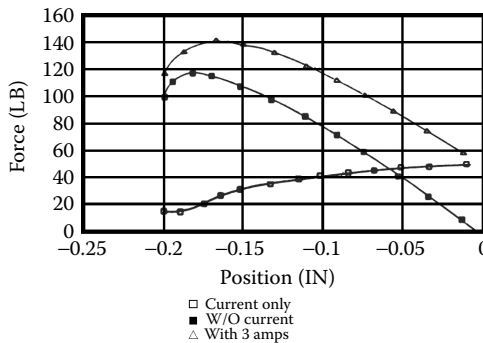


FIGURE 12.28 Force components vs. mover position.

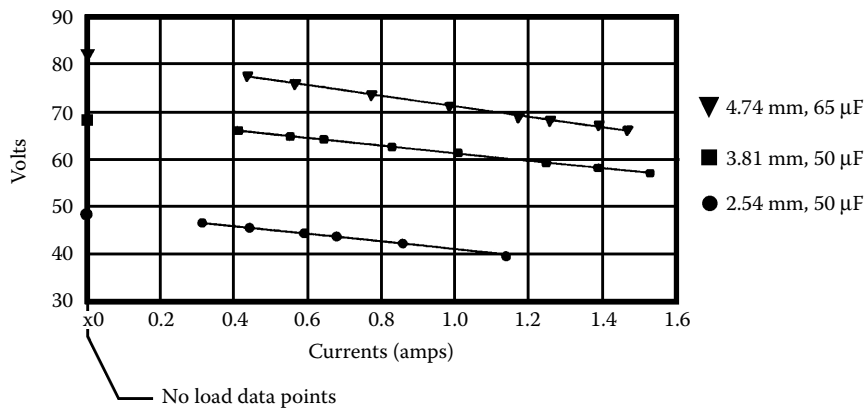


FIGURE 12.29 Alternator voltage vs. current (resistive load) for various constant motion amplitudes and capacitor pairs (test results).

12.10 Control of PM-LMAs

The control of the PM-LMAs depends on the prime mover and load characteristics.

Typical prime movers for PM-LMAs are as follows:

- Stirling engines: free piston engines [16]
- Spark-ignited linear internal combustion engine (ICE) [2]
- Very low-speed reciprocating wave machines (0.5 to 2.0 m/sec average speed) [6]

The control of stroke should be instrumented through the prime mover, while the adaptation of alternator voltage to the load — independent or at power grid — is to be performed by power electronics.

The motion frequency may be maintained constant and equal to power grid frequency, securing operation at the mechanical resonance frequency. If the frequency of motion varies within some limits, then the complexity of power electronics has to increase, if the load demands constant frequency and voltage output.

12.10.1 Electrical Control

So far, we encountered only single-phase linear alternators.

The control solution refers to the following:

- Constant motion frequency applications
- Variable motion frequency applications

They handle the following:

- Stand-alone nondemanding loads
- Strong power grids

For constant motion frequency, the control is decoupled (Figure 12.30):

- Motion amplitude control via prime-mover governor to deliver more or less mechanical power
- Voltage adaptation if operation at the power grid or at a certain voltage is required

The power error is the input of a power regulator followed by the stroke length, l_{stroke}^* , regulator. Finally, the fuel injection rate is modified to produce the required motion amplitude, l_{stroke}^* , according to electric power requirements.

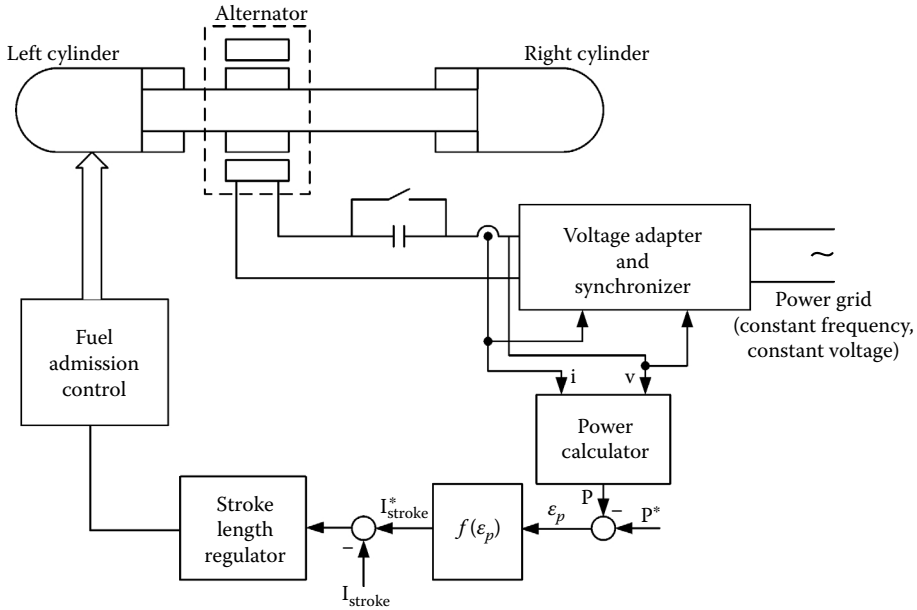


FIGURE 12.30 Control at power grid.

As the frequency is constant, and connection to the grid is required, the motion phase may be modified slowly to provide the synchronization to grid conditions. The voltage adapter may be an autotransformer with variable ratio via a small servomotor. Alternatively, a soft-starter may be used to reduce the transients during connection to the grid. An additional resistance may be added before synchronization, in series with the alternator, and then short-circuited a few seconds later, as is done with wind induction generators.

When the motion frequency is allowed to vary, a front-end rectifier plus inverter may be applied (Figure 12.31). In this case, the series capacitor is no longer required, as the DC link capacitor does the job. The DC link voltage may now be larger than the LMA emf.

A bidirectional converter makes the interface between the variable voltage and frequency of the PM-LMA and the load requirements. The control of the load-side converter is different for independent load in contrast to power grid operation. The subject was presented for three-phase PM generators in Chapter 10. It should be similar for the single-phase alternators. The bidirectional converter also allows for motoring and, thus, could help in starting or stabilizing the prime mover's motion when the operation takes place at the power grid or with a backup battery. In this case, the smooth connection to the power grid is implicit.

Note that for ocean-wave linear translator alternators, three-phase configurations were proposed [6]. When the linear motion changes directions, the phase sequence changes, but the active front rectifier can handle, implicitly, this situation. A three-phase bidirectional converter is required in this case. The control of such a system is similar to the case of rotary PM generators (Chapter 10).

12.10.2 The Spark-Ignited Gasoline Linear Engine Model

The spark-ignited gasoline linear engine model was developed in Reference 8 (Figure 12.30).

The basic force balance equation of the system is as follows:

$$P_L(x)A_B - P_R(x) - F(x) = m_t \ddot{x} \quad (12.71)$$

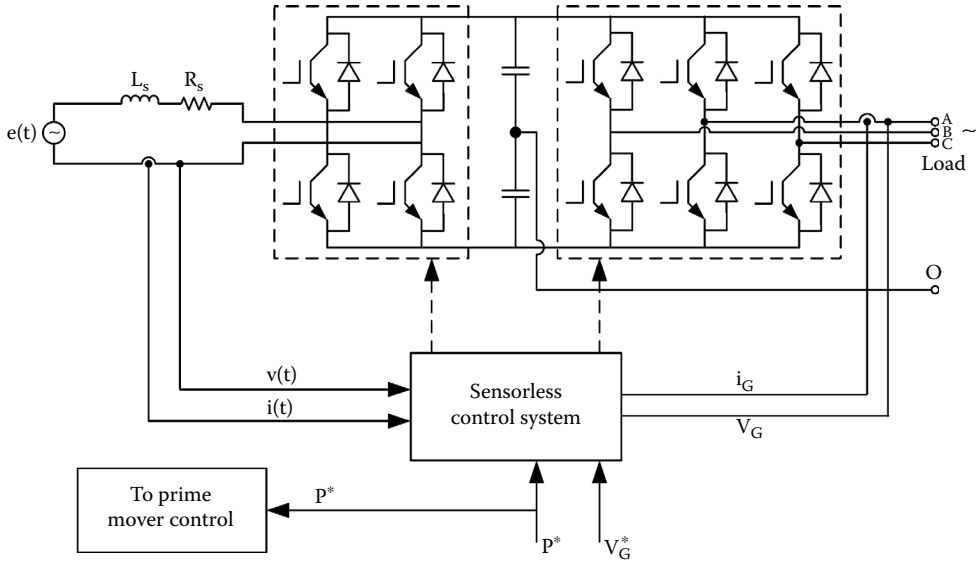


FIGURE 12.31 Permanent magnet linear motion alternator (PM-LMA) with bidirectional power converter.

where

$P_L(x)$ is the instantaneous pressure in the left cylinder

$P_R(x)$ is the instantaneous pressure in the right cylinder

A_B is the bore area

F_x is electromagnetic, friction, and mechanical spring (if any) force

m_t is the total translator mass

Under ideal (frictionless) no-load operation, no heat is required to sustain the motion; the forces from compressing and expanding gas in the engine will maintain motion by themselves.

The natural frequency of the engine is thus met.

In the absence of mechanical springs and of cogging force of the alternator, the force balance at no load becomes [9] as follows:

$$A_B P_1 \left(\frac{2r}{r+1} \right)^n \left[\left(1 + \frac{x}{x_m} \right)^{-n} - \left(1 - \frac{x}{x_m} \right)^{-n} \right] = m_t \ddot{x} \quad (12.72)$$

where

P_1 is the intake pressure

x_m is the mid-position point (half-stroke length: $x_m = l_{stroke}/2$)

r is the compression ratio

Equation 12.72 produces almost harmonic motion [9].

As expected, the real prime mover departs from this situation, and the motion frequency is slightly reduced under load. The presence of mechanical springs (and of cogging force) tends, however, to bring back the frequency to resonance conditions.

12.10.3 Note on Stirling Engine LMA Stability

The stability of an LMA with Stirling engine prime mover in operation at constant voltage is treated in Reference 1. In the absence of mechanical springs and of cogging force, but with full capacitor compensation, the motion frequency is varied, and the steady-state stability condition is checked:

$$W_d < W_e \quad (12.73)$$

where

W_d is the mechanical (input) energy per cycle of ω (frequency)

W_e is the electrical (output) energy per cycle of ω

A complete study of stability with closed-loop stroke control, mechanical springs, cogging force, and with power electronics interface to independent load or at power grid is still to come.

12.11 Progressive-Motion LMAs for Maglevs with Active Guideway

So far, we discussed only LMAs with linear oscillatory motion. The LMAs may also be used with progressive linear motion. A typical case is the magnetically levitated trains (maglevs) with active guideway. By active guideway we mean that the maglev propulsion is provided by linear synchronous motors with superconducting or conventional excitation on board [10, 11, 12]. Auxiliary power on board, in the absence of electric power mechanical collectors, has to be obtained through electromagnetic induction (contactless) and used in corroboration with battery storage.

For the maglev with electromagnetic controlled excitation, the German Transrapid 06 and 08 (Figure 12.32a through Figure 12.32c), the three-phase cable winding, which is located along the guideway (face down), in an open-slot laminated core, is fed in synchronism with the vehicle (excitation rotor poles) such that the emfs are in phase with the phase currents.

The suspension is produced by the control of the excitation current on board from zero speed. The propulsion is controlled from on-ground power stations, provided with variable frequency bidirectional alternating current (AC)–AC power electronics converters.

The armature (stator) mmf, in the presence of open slots, produces an airgap flux density that pulsates visibly with the stator slot pitch periodicity.

With three slots per pole (Figure 12.32b), the pole pitch of the flux density pulsation due to slot opening is $\tau_g = \tau/6$.

If the emf produced by this flux density harmonics is to be extracted, we need to plant, on the salient poles of the inductors on board the maglev, a two- or three-phase winding with the pole pitch τ_g . For a two-phase winding, we need to plant at least six smaller slots on the inductor pole.

For a two-phase winding, three coils per phase are thus obtainable, as the inductor pole span is about $2\tau/3$ (Figure 12.32c).

A diode rectifier with the two phases in series, a filter and a DC–DC converter may represent the solution to interface the linear generator to the battery and to the load in parallel with it.

It was shown that such a linear generator can produce sufficient energy on board for excitation control and for auxiliary loads, above the half-rated speed of the vehicle. Under this speed, the battery takes over. When the vehicle is in a stop station, the batteries are charged from an on-ground energy system.

In essence, the linear generator investigated here is a synchronous linear biphasic machine with independently variable excitation current. It is the task of the diode rectifier plus the DC–DC converter,

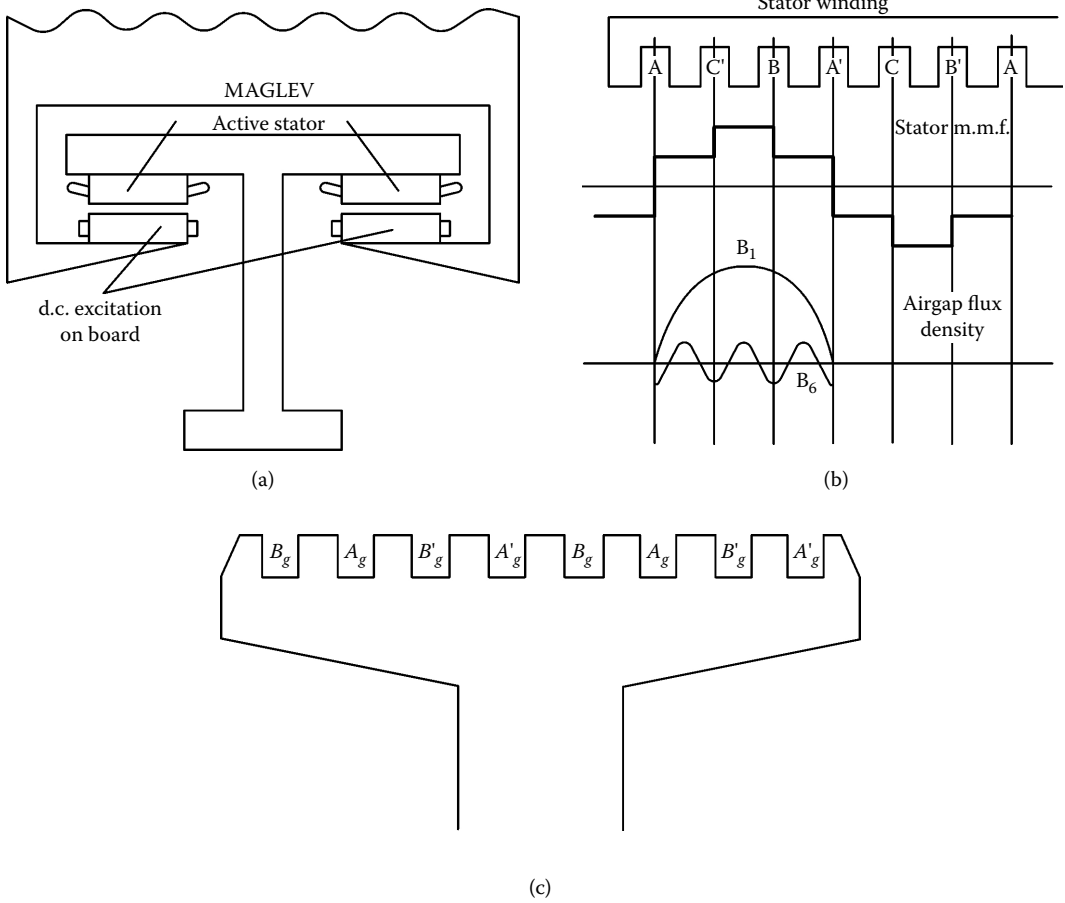


FIGURE 12.32 The Shanghai Transrapid Maglev Line: (a) the structure, (b) open-slot stator armature airgap flux density (B_6), and (c) linear generator winding.

eventually of the boost-buck type, to extract most of the available energy in the slot opening airgap flux density harmonic, produced by the stator current.

For the superconducting Japanese maglev (MLX01) [12], a similar solution may be feasible, as the configuration is somewhat similar (Figure 12.33 [12]).

The track is provided laterally along a U-shaped concrete track, with a three-phase cable propulsion winding with three “slots” per pole and pole pitch τ and figure-eight-shaped levitation short-circuited coils with a span of $\tau/2$ to decouple the two windings.

On board, there is a row of superconducting coils with pole pitch τ .

The superconducting coils on board produce levitation and guidance through the currents induced by motion in the figure-eight-shaped stator short-circuited (levitation) coils. At the same time, they interact with the three stator phase windings for propulsion.

Now it is known that the levitation guidance through induced currents is based on repulsive forces that provide statically stable operation and, within some conditions, some negative damping of levitation and guidance motions.

To further improve negative damping, an actively controlled coil system on board was used [10]. But to use a linear generator for the purpose is better, because both energy production on board and damping are provided by the same hardware.

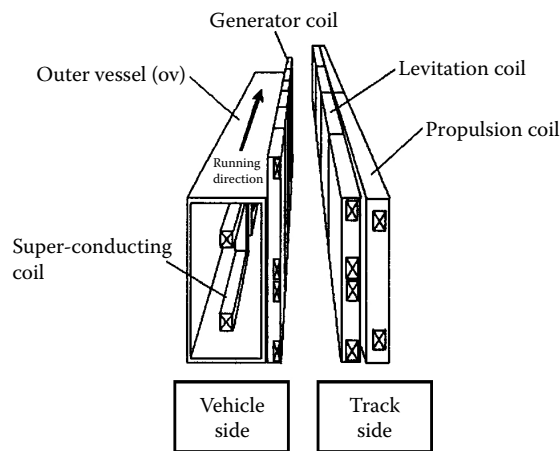


FIGURE 12.33 Superconducting maglev with linear superconducting generator.

This is how figure-eight-shaped coils are placed on board to form a three-phase winding. They have to sense a harmonic field of levitation coil currents. The linear generator makes use of superconducting coils to induce, through vehicle motion, currents in the levitation guidance ground coils. In turn, a harmonic field of the stator levitation coil currents induces emf in the linear generator winding.

A power electronics converter is needed as an interface between the linear generator and the battery (Figure 12.34).

The maximum output of the linear generator to the fixed voltage battery and load increases with speed (Figure 12.35) [12].

Details of the control of such a system are given in Reference 12, with reference to the superconducting maglev MLX01.

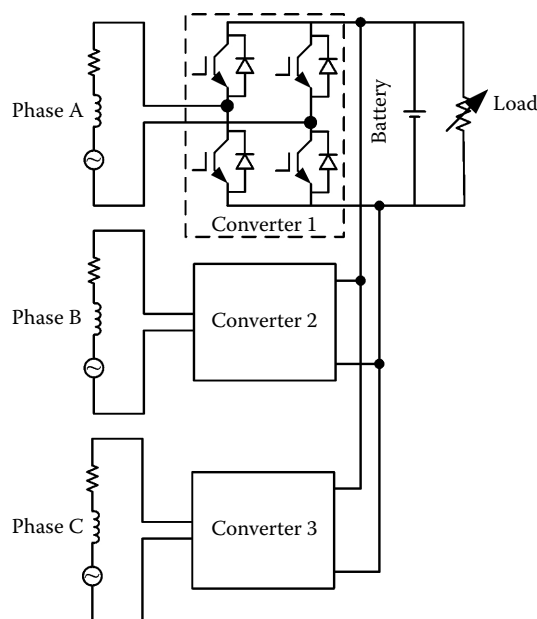


FIGURE 12.34 Three single-phase converters for the superconducting linear generator for maglev.

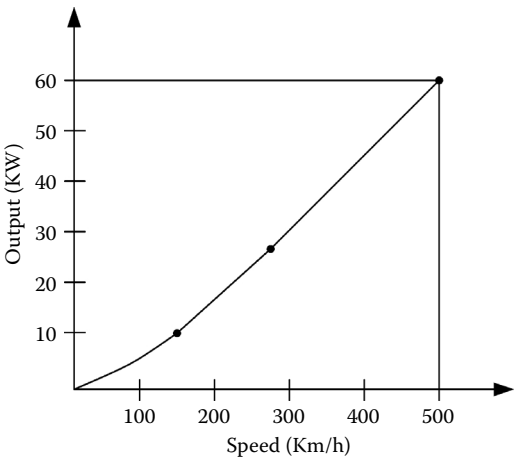


FIGURE 12.35 Output vs. speed of superconducting linear generator on maglevs.

12.11.1 Note on Magnetohydrodynamic (MHD) Linear Generators

The MHD linear generators were proposed decades ago for direct conversion of ionized plasma at 3000 K heat to linear plasma motion at 1000 m/sec or so, and then, to electrical energy in a plasma DC linear superconducting generator (Figure 12.36) [10].

The magnetic field $\underline{B}$ has to be large and, thus, is produced via superconducting magnets. Perpendicular to $\underline{B}$ and to the direction of plasma speed $\underline{u}$, there are two electrodes that collect the emf E_{dc} :

$$E_{dc} = uBL \tag{12.74}$$

Despite the seeded plasma low conductivity ($\sigma_{plasma} \approx 40$ to 50 Siemens), the large speed $u \approx 800$ to 1000 m/sec and the large flux density $B = 4$ T provide for acceptable electrical performance. For adiabatic thermal conditions, efficiency is above 55%.

It is still claimed that such a linear MHD generator could notably improve the total efficiency in thermal power plants. However, since the recent developments of dual-cycle gas turbines with total efficiency above 60%, the future of linear MHD generators for power systems may seem less promising.

For a detailed study of them, however, you may start with Reference 10, Chapter 5.

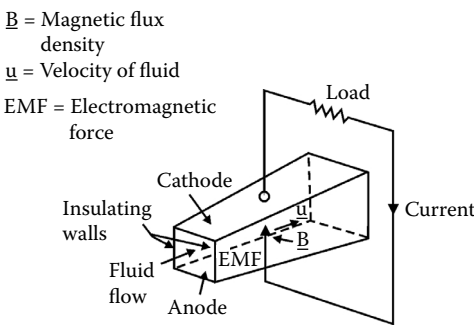


FIGURE 12.36 Plasma direct current (DC) linear magnetohydrodynamic (MHD) generator.

12.12 Summary

- LMAs directly convert linear oscillatory or progressive motion mechanical energy into electrical energy.
- Prime movers for LMAs so far are free-piston Stirling engines, single- or dual-piston gas linear engines, or wave energy engines for oscillatory linear motion.
- Maglev (magnetically levitated) vehicles are typical applications for on-board linear progressive motion alternators that work with battery backup to provide auxiliary power. Also, plasma MHD linear motion energy of seeded hot plasma to directly convert thermal energy to mechanical energy to electrical energy is feasible.
- The oscillatory LMAs are typically single-phase parametric machines, especially if the stroke length is generally below 100 mm.
- It was soon realized that equality between mechanical resonance and electrical frequency is key to high efficiency. Mechanical calibrated springs, as copper–beryllium flexures, are used to store and release mover kinetic energy at stroke ends.
- As the resonance frequency $f_1 = \sqrt{K/m_t}/2\pi$ decreases with mover mass, the reduction of the latter is a problem at $f_1 = 60$ Hz for powers in the kilowatts and tens of kilowatts range for stroke length l_{stroke} well below 100 mm. Consequently, reduction of machine and mover size is paramount. This is how permanent magnets come into play, with the added advantage of higher force per watt of losses.
- So, basically, PM-LMAs are single-phase SGs. Though trapezoidal motion was tried [13], as trapezoidal motion implies small power pulsations, the harmonic (sinusoidal) speed profile is still in favor, mainly due to practical reasons.
- For sinusoidal motion, the PM flux linkage variation in the armature coils has to be linear to secure sinusoidal emf waveform. All efforts to achieve this goal should be made.
- There are numerous LMA configurations of practical interest, and they may be classified with respect to mover type and geometrical shape as follows:
 - With coil mover
 - With PM mover
 - With iron mover
 - Tubular
 - Flat, double sided
- There are many subdivisions of these main types, such as with air coils or in-slot coils or with PM flux paths within the motion plane or transverse to it, or without or with PM flux concentration.
- The aim for high force/volume is in contradiction to high force/watt of losses and to low-power factor angle $\phi_1 = \tan^{-1}(\omega_1 L_s I_n/E)$. The latter is decisive in the power output level for given terminal voltage or in voltage regulation. Full compensation of machine inductance by a series capacitor is not uncommon to maximize the output for given geometry at highest force/watt of losses.
- LMA configurations with PM flux concentration lead to large force/volume and force/watt, but a larger capacitor for reactive power compensation is required. We trade here PMs for capacitors when optimization is performed.
- Air coils, on stator or on mover, have the lowest electrical time constants T_e ($T_e < 10$ msec for powers in the 1 to 50 kW range) at good power factor and moderate force density (4 N/cm² at 22.5 kW).
- PM-mover and iron-mover LMAs are shown to produce the same force density for higher force/watt of losses but at moderate power factors.
- Realistic analytical models for a few configurations were applied for numerical examples of interest, and two of them were validated through FEM analysis. Efficiency above 85% at 100 W output was demonstrated with weight less than 15 kg/kW.
- The control of LMA connected to the power grid was demonstrated to be possible only via stroke control of the Stirling engine prime mover, with a full compensation capacitor [3].

- A more elaborated control is obtained through a bidirectional power electronics converter for interfacing with the load, as much as for the three-phase rotary PM generators ([Chapter 6](#), *Synchronous Generators*).
- Though very large force density (12 N/cm^2) [6] was recently claimed, as for rotary counterparts ([Chapter 11](#)), with some configurations, the corresponding power factor was about 0.2. This means very large capacitor energy storage (for reactive power compensation) that is not easy to justify.
- Low cost and advanced power electronics control was introduced by LMAs [13].
- For progressive linear motion, maglev linear generators were proposed in the tens of kilowatts power range. They exploit the airgap flux density space harmonics of stator coils, be them the three-phase winding or the levitation short-circuited coils. With proper battery backup, they were shown to be able to cover the energy needs on board active guideway maglev vehicles.
- Seeded plasma linear progressive motion MHD DC brush generators were also proposed to improve the overall efficiency in thermal power plants above 50% without low energy heat delivery considered (10% over standard turbines). As the dual-cycle gas turbines, introduced recently, pushed the overall efficiency (thermal plus electrical) above 60%, the MHD generators may not aggressively enter power systems anytime soon. See Reference 10 for a detailed introduction to linear MHD generators.

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